This dissertation presents an assessment of the worst case stresses produced in an induction motor when the motor is allowed to ride through a power supply voltage disturbance. Results from laboratory experiments and computer simulations are shown. The experimental results are obtained from tests conducted on three squirrel cage induction motors, sized 10 hp, 50 hp, and 75 hp. Each motor is tested with three different load inertias, various motor loadings, and numerous interruption durations. The computer simulation results are obtained using a non-linear motor-load model, whose parameters are derived from a non-linear least squares parameter estimation technique. Experimental data acquired in the lab is used for the parameter estimation data and for the validation data. Deficiencies in the standard motor-load model are presented and addressed. Good agreement between the experimental data and the non-linear motor-load model data is achieved. Results show significant current and torque transients, but caused no significant damage to the motors or loads used for testing.
ASSESSMENT OF STRESSES ON INDUCTION MOTORS DUE TO MOMENTARY SERVICE INTERRUPTIONS

by

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DEDICATION

This dissertation is dedicated to my very patient and supportive fiancée, Sheila Hughes, and to my wonderful family.
BIOGRAPHY

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Chapter 1

Introduction

Three phase induction motors are critical components of nearly all industry facilities. Induction motors are commonly controlled by contactors, which are electromagnetic switches that are highly sensitive to voltage sags and momentary service interruptions. Many contactors open if the voltage falls below 50% of nominal for longer than one cycle [1] [2] [3]. A voltage sag or momentary service interruption which simply causes the lights to flicker in an office will often cause one or more contactors to open. An entire facility can shut down if a critical motor stops because the contactor controlling it opened. In some cases, simply re-closing the affected contactor is all that is needed. But in other cases, such as in continuous process facilities, costly time consuming start-up procedures are required.

In recent years, several products were developed to eliminate a contactor’s sensitivity to voltage sags and/or momentary service interruptions [2] [4]. These contactor ride-through devices keep contactors closed during voltage sags and momentary service interruptions that would otherwise open the contactors. Unfortunately, this solution raises concern with motor and equipment manufacturers who claim that damaging mechanical stresses are generated when a contactor controlling a motor is held closed during a voltage sag or momentary service interruption.

1.1 PROBLEM STATEMENT

There is an industry need and a desire to employ ride-through devices on contactors that control critical induction motors. Costly, unintentional shut downs can be avoided by the use of a cheap, simple, easy to install and maintain contactor ride-through device. However, motor and equipment manufacturers discourage their use, so industry is reluctant to adopt the technology. Very little experimental data and conflicting analytical data is currently available to support motor and equipment manufacturers claims, as the first part of this study will show.
1.2 OUTLINE

In an effort to investigate the claims of motor and equipment manufacturers, this research work presents an analysis of the stresses produced in induction motors resulting from the use of contactor ride-through devices. The focus is placed on the worst case current and shaft torque transients generated during a power supply disturbance. The approach is broken down into four main parts:

1. A review of the literature for relevant prior work.

2. Single motor experimental tests conducted on three induction motors. The motors are 460 V, 4 pole, squirrel cage induction motors sized 10 hp, 50 hp, and 75 hp

3. Single motor simulation tests conducted on the three motors used in part 2. A parameter estimation method for estimating the induction motor model parameters and the load model parameters from the experimental data is developed. The models are used to help describe and analyze the measurement data and extend the study to a multi-motor system.

4. Multi-motor simulation tests conducted on a system of motors employing contactor ride-through devices. Motor parameters from part 3 above are used in the model.

1.3 BACKGROUND

A basic motor control circuit is shown in Fig. 1 [5]. A single motor, MOTOR1, is controlled by a single start-stop station consisting of two push-button switches, PB1 and PB2. Pressing PB2 closes the contactor and energizes the motor and pressing PB1 opens the contactor and de-energizes the motor.

Once the contactor is opened, the stator circuit currents stop flowing and the motor rotor begins to decelerate. If the motor rotor and/or load inertia is high, the motor rotor may slowly decelerate and not come to a complete stop for some time. Although the stator circuit is disconnected from the voltage supply, a voltage is still present at the motor terminals. This voltage, or back-emf, is induced by the currents continuing to flow in the spinning rotor. The
frequency of the back-emf decreases as the rotor speed decreases and the magnitude of the
back-emf decays as the magnitude of the rotor currents decay. This is shown in Fig 2, where
the motor voltage, the motor current, the shaft torque, and the rotor speed of a 10 hp
induction motor are acquired during a motor stop. At time t=0, the contactor is opened.
Before time t=0, the motor was operating at steady state, rated conditions.

Figure 1 Basic motor control circuit
In addition to the normal mechanisms for opening a contactor, a voltage sag or momentary service interruption can also open a contactor. However, this is a highly undesirable and unintentional contactor operation. A contactor ride-through device installed as shown in Fig. 3 can prevent these unintentional contactor operations. The contactor ride-through device, CRD1, stores enough energy to supply the contactor coil, C1, during specified voltage sags and/or momentary service interruptions. The contactor therefore remains closed during the voltage disturbance.

When a contactor ride-through device is used, the motor remains connected to the voltage supply during the voltage sag or momentary service interruption. This results in the back-emf of the motor interacting with the voltage from voltage sags or momentary service interruptions. Since a three phase induction motor is designed to operate on balanced, sinusoidal, periodic, steady-state voltage, the potentially unbalanced, non-sinusoidal, non-
periodic voltage sag or momentary service interruption can cause significant current and torque transients. Equipment and motor manufacturers suggest that these transients can be severe, can damage their products, and as a consequence have recommended not installing contactor ride-through devices.

Figure 3 Motor control circuit with contactor ride-through device
Chapter 2

Prior Work

2.1 STRESS ASSESSMENT OF INDUCTION MOTORS

The concern of motor and equipment manufacturers is excessive mechanical stresses that are possibly produced when an induction motor is supplied by polluted voltage. It is therefore important to identify the weakest part of the motor and quantify how much stress it can tolerate and how much stress is produced by supplying the motor with the voltage resulting from a power supply disturbance. With these two factors known, an informed decision can be made on the use of contactor ride-through devices.

The most common area of failure due to excessive mechanical stresses in an induction motor is the coupling keyway area of the shaft [6]. A determination of the mechanical stresses that can be tolerated by the coupling key is a good way to determine the stresses that can be tolerated by the motor, since the coupling keyway area is the weakest part of the machine.

The coupling key prevents the coupling from rotating on the motor shaft and is therefore responsible for locking the coupling in place so that the torque developed by the motor can be transmitted to the mechanical load. The coupling key is exposed to any and all torque transients developed by the motor and transmitted to the load. The torque required to yield the shaft key can be calculated from

\[
\text{Torque}(\text{in-lb}) = \text{Yield Strength}(\text{psi}) \times \text{Shaft Radius}(\text{in}) \times \text{Key Width}(\text{in}) \times \text{Key Length}(\text{in})
\]  

(2.1)

where Torque(in-lb) is the torque in in-lb applied to the motor shaft. From (2.1) it is clear that the shaft torque is an important measure of mechanical stresses that can be tolerated by an induction motor.
2.2 POWER SUPPLY DISTURBANCES

The first step in investigating the concerns raised by equipment and motor manufacturers is to investigate the types of power supply disturbances that an induction motor can be supplied with if a contactor ride-through device is used. According to [7], there are eight types of voltage irregularities, or “pollutants,” that are present on the utility grid. They are:

1. voltage level variation from nominal
2. frequency variation
3. voltage unbalances between phases
4. distortions in sine wave voltages and currents
5. switching surges and other transient voltages
6. flicker, single phase and poly-phase
7. supply fault disturbances
8. momentary service interruptions

Of the eight “pollutants,” only voltage level variations from nominal, supply fault disturbances and momentary service interruptions are relevant to contactor ride-through applications. These three pollutants can originate at the transmission level, the distribution level, or at the utility customer level and can last a fraction of a cycle or longer. In all three cases, the supply voltage at the point of use will sag or completely drop to zero. Under normal contactor operating conditions, any one of these 3 pollutants can cause a contactor to open. However, if a contactor ride-through device is used, the contactor will remain closed. It is therefore important to understand induction motor behavior if the motor is supplied with a voltage variation from nominal, the voltage resulting from a supply fault disturbance, and the voltage during a momentary service interruption. The remaining 5 pollutants are not considered because they do not normally cause a contactor to open and are not relevant to contactor ride-through applications.

2.3 EXPERIMENTAL STUDIES

Of particular interest to this study is previous experimental work that measures the shaft torque during any of the three voltage supply pollutants discussed above. However, very little experimental data is available. The most relevant work is found in [8] and [9], where the first experimental torque transient analysis of a loaded induction machine subjected to a momentary service interruption is presented in 1967 and 1969, respectively. In 1965, [10] reveals the first experimental torque transient data due to a momentary service interruption,
but the results are only for one momentary service interruption duration using a 0.75 hp unloaded motor. In 1979, [11] presents experimental data for the case of out of phase bus transfer of a 111 hp induction motor, but only current transient data is provided. In 2001, [12] investigated the effects of a momentary service interruption and a supply fault disturbance on a 7.4 hp induction motor, but again, only current transient data is provided. These are the only experimental studies that address induction motor behavior during momentary service interruptions, voltage sags, or supply vault disturbances. A closer look at [8] [9] and [12] follows.

In 1967, I.R. Smith, et al perform the first experimental torque transient analysis of a loaded induction machine subjected to a momentary service interruption [8]. The experimental data is obtained from a 7.5 hp three-phase induction motor connected to a dc generator. To measure shaft torque, strain gages are fastened to the motor shaft and fed into a transistor amplifier through a set of slip rings. A momentary service interruption is generated by manually switching off the supply voltage and then reconnecting it before the rotor stops spinning. The authors call this process "reswitching". Fig 4 shows experimental and analytical results of removing the supply voltage for 4.65 cycles. The analytical results are computed using the dq0 reference frame model, as shown in Appendix A. Torque transient peaks of 5.5 times full load torque (5.5 pu) and transient current peaks of 13 times rated current (13 pu) are measured and match analytical results reasonably well. Although not shown in Fig 4, tests are also performed where the supply voltage is reconnected at the instant the back-emf and supply voltage are in phase. In this case, the magnitude of the torque transients are significantly less than the magnitude of the transients seen in Fig 4.

This study clearly shows that torque and current transients are produced if a motor is allowed to ride-through a momentary service interruption. The torque during this test actually reverses direction, indicating that the shaft decelerates in order to realign the rotor field with the stator circuit flux magnetic field. Unfortunately, the results of only one interruption duration are published and tests are only conducted on one motor at full load.
In 1969, Slater, Flynn and Wood present a paper focusing on the negative torque peaks developed by an induction motor during a momentary service interruption [9]. Their analysis includes both analytical results and experimental results. The analytical results are also obtained using the dq0 reference frame model. The experimental results are obtained using a three-phase, 5 hp induction motor. Shaft torque is computed from the measured acceleration and inertia. The negative torque peak resulting from a momentary service interruption is plotted as a function of the angle between the supply voltage and the back-emf of the motor at the instant of reconnection, as shown in Fig 5. The greatest torque transients are developed when the angle between the line voltage and the back-emf of the motor is about 200 degrees. Figure 6 shows a comparison of the experimental and analytical results. There is some discrepancy between the simulated and measured results in magnitude of the torque and the phase angle at reconnection that produces the highest torque magnitude.
Figure 5  Measured 5 hp induction motor negative torque peak magnitude vs reconnection phase angle. Figure 1 in [9]
Figure 6  Measured and computed negative torque peak magnitude vs reconnection phase angle. Figure 4 in [9]

Although the general behavior of the results in [8] and [9] agree, the magnitude of the peak shaft torque in [9] is nearly twice that of the peak shaft torque magnitude in [8]. These are the only two publications that include experimental torque transient data and [9] did not directly measure the shaft torque. Instead, the shaft acceleration is measured and the torque is computed.

Slater, Flynn, and Wood model the experimental behavior they observe in [9] and use the model to extend the study to a larger motor with three different load inertias. The load inertia is shown to greatly influence the magnitude of the negative torque peak and the interruption duration at which the worst peak occurs. These are valuable results, but they are not verified
experimentally. [11] also reports experimental results with two different load inertias, but no torque data is provided.

Although [12] does not include any shaft torque data, the interruption duration is shown to greatly affect the magnitude of the motor current. Also shown are results demonstrating that the magnitudes of the current transients generated during a momentary service interruption are greater than the magnitude of the transients generated during a supply fault disturbance.

2.4 ANALYTICAL STUDIES

Induction machine current and electromagnetic torque transients have been analyzed since the 1940’s [13]. Early efforts focused on starting transients. Later, efforts focused on induction motor transients due to momentary service interruptions and supply fault disturbances. A frequently used analytical model for studying the induction motor transients is the dq0 reference frame model, described in Appendix A.

2.4.1 Solutions Using the dq0 Frame

The first published analysis of induction motor transients due to a supply fault disturbance and a momentary service interruption was in 1944 [14]. F.J. Maginnis and N.R. Schultz, analyze the effect of "electrical torques due to sudden change in the applied voltage" and "reclosing of stator circuit with trapped flux in the rotor". Using a Differential Analyzer, the analysis is carried out for a three-phase induction motor.

Fig 7 and Fig 8 show the electrical torque and speed characteristics, respectively, due to suddenly changing the supply voltage [14]. In both figures, before time t=0, the motor is running at steady state conditions with a constant torque load. In Run 26, the stator supply voltage is suddenly dropped to zero at time t=0, simulating a short at the motor terminals. For Run 32, the supply voltage is reduced to half of the steady state value. In both cases, the supply voltage is restored to the nominal value after nine cycles. Figure 7 shows that suddenly dropping the supply voltage to zero volts causes a negative torque peak of five times the rated steady state torque, or -5 pu. A sudden drop to half of the supply voltage only causes a negative torque peak of -2 pu. Clearly, the magnitude of the torque transients are worse for the case of shorting the motor terminals. Several more runs are made where the
interruption time is fixed, but the electrical angle in which the supply voltage is removed or reapplied is varied. The authors report that there is no significant change in the electrical torque due to varying this angle. This means that the point on wave in which the supply voltage is interrupted is not as critical as the duration of the interruption.

Figure 7  Computed electrical torque due to a supply fault disturbance. Figure 4 in [14]

Figure 8  Computed rotor speed due to a supply fault disturbance. Figure 5 in [14]

Figures 9 and 10 show the transient electrical torque and the speed, respectively, due to a momentary service interruption. For this case, before time \( t=0 \) the motor is running at steady state conditions with a constant torque load. At time \( t=0 \), the supply voltage is removed. Several cycles later, the supply voltage is reapplied, as indicated in Fig 9. A negative transient torque peak of \(-9\) pu is produced when the supply voltage is reapplied after a nine cycle dropout. Clearly, the magnitude of the torque peaks shown in Fig 9 are much greater than the magnitude of the torque peaks shown in Fig 7. This suggests that a momentary service interruption produces more torque and is more stressful to a motor than a fault at the motor terminals. Also, delaying the reapplication of supply voltage to ten cycles almost completely removes the negative torque transient. This shows that in this case, there is at least one optimum point to reapply the supply voltage in order to minimize the electrical torque transients due to a momentary service interruption. This behavior agrees with the experimental findings of [8] and [9].
Figure 9  Computed electrical torque due to a momentary service interruption. Figure 13 in [14]

Since the analytical results and experimental results in Fig 6 agreed reasonably well, the authors of [9] extend the analysis to larger motors. Fig 11 shows the analytical solution extended to a 100 hp induction motor with three different load inertias. The per unit magnitude of the negative torque peak (measured torque / rated torque) is plotted as a function of the interruption duration. Fig 11 shows that severe negative torque transients only occur at certain interruption durations, as also reported by [14]. This behavior is expected. Fig 5 from the previous section shows that the magnitude of the negative torque peaks will be greatest when the supply voltage is reapplied at the instant the back-emf from the motor and the supply voltage are about 200 degrees out of phase. Since the rotor speed
decreases once the supply voltage is removed, the frequency of the back-emf also decreases. Therefore, for certain interruption durations, the supply voltage will be reapplied to a back-emf that is in phase. At other interruption durations, the supply voltage will be reapplied to a back-emf that is out of phase. The magnitude of the worst torque peak and the interruption duration at which it occurs are a function of the motor and load inertia. Larger system inertia causes the rotor speed to decreases more slowly, so the motor back-emf and supply voltage are in phase for a longer duration, as shown in Fig 11. Surprisingly, Fig 11 also shows that the magnitude of the negative torque transient decreases as the load inertia increases. This is possibly because the energy stored in the rotor decreases as the interruption duration increases.

Figure 11 100 hp computed negative torque peak for various load inertias. Figure 6 in [9]

In 1997, Richards and Laughton characterize induction motor shaft torque transients based on motor parameters [15]. The shaft torque is computed using an undamped second order model of two masses connected by a flexible shaft. Fig 12 shows the results. The supply voltage is reconnect at time t=0 after a momentary service interruption.
According to this model, the shaft experiences several cycles of periodic oscillations that do not appear to decay. Also, no negative torque transients are shown in Fig 12 or mentioned in [15]. This does not agree with the experimental results published by [8] and [9]. In fact, the experimental shaft torque waveform in Fig 4 is not even similar to the shaft torque waveform in Fig 12. Since the two mass model does not produce results similar to experimental results, it is most likely an inadequate model.

Other authors either use variations on the dq0 reference frame model or variations on the system model to improve their analytical results.

The authors of [16] include the effect of magnetic saturation. They find that accounting for magnetic saturation significantly decreases the magnitude of the predicted torque transients. In one case, the linear model predicts a peak positive torque and a peak negative torque that is 14 percent higher than what the saturated model predicts.

The largest predicted torque transient magnitude is found by Akbaba [17]. His analysis includes an induction motor connected to a distribution transformer. The motor remains connected to the secondary side of the transformer while the switching is performed on the primary side. The switching takes place as follows. The supply voltage is briefly applied to the primary side, removed, and then reapplied. The first application lasts for only 10ms, 20ms, or 30ms. The time delay between the initial voltage application and final voltage application is varied. Results are reported for both a 7.5 hp induction motor and a 500 hp induction motor. The magnitude of the maximum torque peak found is 44 pu.
In [18], Kalsi examines the effects of momentary supply interruptions on large deep-bar rotor induction motors using a constant speed solution. His analytical approach assumes that the rotor speed does not decrease while the line voltage is removed. This assumption causes inaccurate results and the author admits that a constant speed approach should not be used when solving for transients due to momentary service interruptions. Experimental and analytical data for the torque transients are not provided since the experimental and analytical data didn’t agree.

In [19], Landy, Levy, McCulloch, and Meyer use an improved deep-bar model and show that the deep-bar properties increase the magnitude of the transient peaks during a momentary service interruption. Figure 13 shows the removal of the supply voltage for 8 cycles. Deep-bar properties are not included in Fig 13. Fig 14, on the other hand, shows the results of the same simulation with deep-bar properties included. Obviously including the deep-bar properties increases the magnitude of the predicted torque transient.

![Figure 13](image1)

**Figure 13** Computed torque and speed transients due to a momentary service interruption. Figure 4 in [19]

![Figure 14](image2)

**Figure 14** Computed torque and speed transients with deep-bar properties due to a momentary service interruption. Figure 5 in [19]
2.4.2 Solutions Using the abc Frame

In 1995, Shaltout and Al-Omoush reported that a supply fault disturbance produces more severe transients than a momentary service interruption [20]. This does not agree with the results of [14], which state the exact opposite. Fig 15 shows the computed shaft torque transient resulting from a supply fault disturbance. Fig 16 show the computed shaft torque transient resulting from a momentary supply interruption. Clearly the magnitude of the shaft torque transient is greater in Fig 15. However, the shaft torque waveform in both figures is similar to the shaft torque waveform in Fig 12, which does not agree with previously published experimental data. These results also use the two mass model as the motor/load model, which is most likely incorrect. Not only is there significant oscillation following the supply voltage disturbance, but there is also significant oscillation before the disturbance. A three phase induction motor/load should not produce such significant oscillations at steady state conditions.

![Figure 15](image1.png)

**Figure 15** Computed shaft torque transient due to a supply fault disturbance. Figure 4 in [20]

![Figure 16](image2.png)

**Figure 16** Computed shaft torque transient due to a momentary service interruption. Figure 7 in [20]
In 1982, Daugherty performs an analytical study of a 250 hp three phase induction motor [21]. Results for opening all three phases simultaneously and results for using a three phase breaker model that opens each phase at the current zero are presented. The length of the service interruption is varied by a given number of cycles. For the case where the current in all three phases is interrupted simultaneously at a non-zero crossing instant, the maximum peak shaft torque is found to be 23.4 pu. For the case of interruption at a zero crossing, the maximum peak shaft torque is found to be 11.5 pu, as show in Fig 17.

Again, the shaft torque waveforms are generated using the two mass model and are similar to the waveforms in Figs 12, 15, 16, all of which do not match experimental results.

![Figure 17 Computed shaft torque and electrical torque due to a momentary service interruption. Figure 10 in [21]](image)

M.R Chidambara and S. Ganapathy present analytical results for an induction motor subjected to a momentary service interruption [22]. Fig 18 shows the computed electric torque for various damping factors ("γ") and for various ratios of actual speed to synchronous speed ("n"). The damping factor is a constant based on the motor parameters. Minimal torque oscillations due to a momentary service interruption is achieved by an "n" equal to 0.90 and a "γ" equal to 0.4. Or in other words, a loaded, damped motor produces less torque oscillations than an unloaded, un-damped motor. Likewise, more severe oscillations are
caused by an unloaded, un-damped motor. The greatest torque peak is caused by an unloaded, damped motor and is reported to be about four times the steady state torque, in the negative direction. This torque value is much smaller than the electric torque predicted by the previously presented results.

Figure 18  Computed electrical torque due to a momentary service interruption. Figure 2 in [22]
Clearly, the results of the analytical studies vary greatly. Some results predict heavy torque oscillations after a momentary service interruption while other results predict no oscillations. In all cases, the magnitude of the peak negative torque differs greatly. One study even reports that the peak torque magnitude can be as high as 44 pu, but the study that best matches the existing experimental data reports a 9 pu peak torque magnitude. Analytical results modeling deep bar properties and magnetic saturation show a wide variation in results also. However, the studies that best match the experimental data indicate that a momentary service interruption produces more severe torque and current transients that a supply fault disturbance.

2.5 STANDARDS

Four industry standards, NEMA MG 1, IEEE C37.96, ANSI C50.41, IEEE C37.92, provide guidelines for avoiding potentially damaging transients due to momentary service interruptions and supply fault disturbances. Specifically, the standards suggest when the supply voltage can safely be reapplied to an induction motor following a momentary service interruption or supply fault disturbance.

2.5.1 NEMA MG1 - Motors and Generators

The NEMA MG 1 standard was created to assist users with the selection and application of motors and generators. The scope of MG1 is applicable to almost all machines found in industry. Induction motors are covered in detail and therefore momentary supply interruptions are addressed.

As shown in the previous sections, momentary supply interruptions can produce transient torques exceeding the rated steady state full load value. MG1 states that a momentary supply interruption can produce transient torque magnitudes of 2 to 20 times the rated value [23]. For this reason, NEMA recommends only exposing an induction motor to slow transfers or reclosures so that the possibility of damage due to transients are minimized. A slow transfer or reclosure is defined as “one in which the length of time between disconnection of the motor from the power supply and reclosing onto the same or another power supply is equal to or greater than one and a half motor open-circuit alternating-current time constants.”
When an induction motor is open circuited, the rotor flux linkages produce a back-emf at the stator terminals. The motor open-circuit alternating-current time constant is a measure of the time required for the back-emf to decay and is defined by (2.2).

\[ T_{do}'' = \frac{X_M + X_2}{2\pi f r_2} \text{ (Seconds)} \]  

(2.2)

where:

- \( r_1 \) = Stator DC resistance per phase corrected to operating temperature
- \( r_2 \) = Rotor resistance per phase at rated speed and operating temperature referred to stator
- \( X_1 \) = Stator leakage reactance per phase at rated current
- \( X_2 \) = Rotor leakage reactance per phase at rated speed and rated current referred to stator
- \( X_M \) = Magnetizing reactance per phase
- \( f \) = Rated frequency in Hertz

The above parameters are based on the equivalent circuit shown in Fig 19.

MG1 defines a fast transfer as “one which occurs within a time period shorter than one and a half open-circuit alternating-current constants” and recommends that any induction motor exposed to a fast transfer or reclosure should be studied in detail. However, a method or procedure for studying the motor under transient conditions is not given. To aid in the study, motor manufacturers are encouraged to provide the parameters from Fig 19 and any other parameters that might be necessary for a transient study (rotor inertia, shaft spring constant).
2.5.2 ANSI / IEEE C37.96 - Guide for AC Motor Protection

The ANSI/IEEE C37.96 standard was created to illustrate the appropriate motor protection method based on the motor’s size, application, and type. Like NEMA MG1, the scope of this standard applies to most motors found in industry. Guidelines for protection against sags and out of phase reclosing are included.

According to ANSI/IEEE C37.96, most voltage sags last only 5-15 cycles and in most cases an induction motor will not be damaged if it remains connected to the line during such sags [24]. On the other hand, the standard recognizes that unusually high transient currents and torques may occur if an induction motor is subjected to a momentary service interruption. To avoid the transients caused by a momentary service interruption, ANSI/IEEE C37.96 recommends a fast transfer (less than 10 cycles), parallel transfer, residual voltage transfer, slow transfer (greater than 20 cycles), or in-phase transfer. If none of these five options are possible, the standard recommends a transient study be performed. But, like MG1, no recommendation or guideline for the transient study is provided. Also, strictly following ANSI/IEEE C37.96 may not protect the motor from severe transients, as severe transients have already been demonstrated for a momentary service interruption of less than 10 cycles.

2.5.3 ANSI C50.41 – Polyphase Induction Motors for Power Generating Stations

The scope of ANSI C50.41 is more specific than the scope of MG1 and C37.96. Like the title suggests, C50.41 applies to three-phase induction motors intended for use in power generating stations. However, the recommendations given for induction motor momentary service interruptions can be applied to induction motors outside of power generating stations.

C50.41, like MG1, suggests that the peak torque and current magnitudes during a momentary service interruption can reach 2 to 20 times the rated values [25]. For this reason, C50.41 recommends that momentary service interruptions be avoided altogether, since each transient even will reduce the life expectancy of the induction motor “by some finite value.” However, the standard points out that detailed transient studies can be performed in order to determine the actual magnitude of the transient peaks. C50.41 recognizes that these studies are quite complex and require detailed knowledge about the motor/load system. Although
momentary service interruptions are not recommended, if an interruption can not be avoided, C50.41 recommends either a slow transfer or reclosing or a fast transfer or reclosing.

A slow transfer or reclosing is one in which the supply voltage is not reconnected to the induction motor until the rotor currents have decayed to a value that will not produce unacceptable transients. A fast transfer or reclosing is defined as one that meets the following requirements:

(a) Occurs within a time period of 10 cycles or less
(b) The maximum phase angle between the motor residual volts per hertz vector and the system equivalent volts per hertz vector does not exceed 90 degrees
(c) The resultant volts per hertz between the motor residual volts per hertz phasor and the incoming source volts per hertz phasor at the instant of transfer or reclosing is completed does not exceed 1.33 per unit volts per hertz on the motor rated voltage and frequency basis.

The length of time required for a slow transfer can be estimated by a calculation of “the resultant volts per hertz vector between the motor residual volts per hertz vector and the incoming source volts per hertz vector at the instant the transfer or reclosing is completed.” The resultant volts per hertz vector can be calculated by (2.3), which is derived from Fig 20.

Figure 20 Resultant volts per hertz vector. Figure 2 in [25]
\[ E_R = \sqrt{E_S^2 E_M^2 - 2E_S E_M \cos(L)} \]  

(2.3)

where:

\[ E_S = \text{System equivalent per unit volts per hertz} \]
\[ = \frac{\text{System voltage in per unit of motor rated voltage}}{\text{System frequency in per unit of rated frequency}} \]

\[ E_M = \text{Motor residual per unit volts per hertz} \]
\[ = \frac{\text{Motor voltage in per unit of motor rated voltage}}{\text{Motor speed frequency in per unit of synchronous speed}} \]

\[ E_R = \text{Resultant vectorial voltage in per unit volts per hertz on the motor rated voltage and frequency base} \]

The quantities \( E_S \), \( E_M \), and \( E_R \) are normalized by the frequency to account for the phase difference between the back-emf of the motor and the supply voltage caused by rotor speed reduction.

According to C50.41, if the resultant volts per hertz vector calculated using (3) is less than 1.33 per unit volts per hertz, then a motor can safely be reconnected to the supply. However, several sources find that even if this recommendation is followed, severe current and torque transients may still be produced [20][21][26][27].

The standard also suggests that an induction motor can safely be connected to the supply voltage if 1.5 open-circuit alternating current time constants have elapsed, as mentioned in MG1.

**2.5.4 ANSI / IEEE C37.92 – Guide for Induction Motor Protection**

The final standard that addresses momentary service interruptions is ANSI/IEEE C37.92. This standard was published in 1969, is no longer valid, and is currently unavailable. No recommended replacement is given either. While the standard was current, it recommended waiting at least 15-30 cycles before reconnecting an induction motor to the supply [28]. This recommendation was based on a series of experiments on 100-1500 hp motors [29].
The three current industry standards, NEMA MG 1, ANSI C50.41, IEEE C37.96, include information about induction motors that are subjected to momentary service interruptions. NEMA MG1 and ANSI C50.41 suggest that peak torque and peak current magnitudes in an induction motor can reach values of 2 – 20 pu during momentary service interruptions. IEEE C37.96 suggests that peak current magnitudes in an induction motor can reach 2.5 times locked rotor current during momentary service interruptions. All three standards provide guidelines for avoiding the potentially damaging transients due to momentary service interruptions, but independent research shows that significant torque and current transients are still possible even if the recommendations in the standards are followed.

2.5.5 Summary

Industry standards and previous analytical data suggest a wide range of peak current and peak torque magnitudes. Industry standards claim that the peak torque and peak current magnitudes can be as low as 2 pu and analytical studies claims that the peak torque magnitude can be as high as 44 pu. Other studies claim peak torque magnitude values between these two extremes, but the results still vary greatly. Little experimental data is available to determine which studies are most accurate. However, important information found in the literature can help guide this study to determine the worst case shaft torque transients. This information is as follows.

The heating effect of the interruption currents are less than the heating effects of the startup currents and should not cause any adverse problems [12].

Momentary service interruptions produce a higher shaft torque than supply fault disturbances and supply fault disturbances produce higher shaft torque than voltage sags.[12][14][24]

The interruption duration during a momentary service interruption has a significant effect on the shaft torque [8][9][12][14].

The point on wave “drop-out” point of a momentary service interruption has no effect on the shaft torque [14].
The load inertia has a significant effect on the shaft torque during a momentary service interruption [9][11].

Using an undamped, two mass motor/load model does not agree with the previously published experimental results [15][20][21].

Accounting for magnetic saturation produces less electromagnetic torque than neglecting magnetic saturation [16].

Accounting for induction rotor deep bar properties produces more electromagnetic torque than neglecting deep bar properties [19].

The experimental data presented above is valuable, but limited. Only small motors were used and only one study actually measured the shaft torque using strain gauges attached to the shaft. Due to the limited experimental data and conflicting analytical data available in the literature, this study first acquires a significant amount of experimental data. The effect of motor loading, interruption duration, load inertia, and motor size are all evaluated and quantified experimentally.

The prior work indicates that a momentary service interruption produces the highest torque transient magnitudes of the three types of power supply pollutants described in the previous section. Since the goal of this study is to investigate the worst case scenario shaft torque transients associated with using a contactor ride-through device, the experimental work will focus on momentary service interruptions.
Chapter 3

Experimental Based Stress Assessment of Single Motors

The experimental work is conducted on three 4 pole motors, a 10 hp motor, a 50 hp motor, and a 75 hp motor. However, before any of the testing began, a device that is capable of producing momentary service interruptions for these size motors is constructed. Next, the preliminary experimental work is performed and finally, a comprehensive data set is collected.

3.1 POWER DISTURBANCE GENERATOR

In order to conduct a comprehensive experimental study on the transients developed during a momentary service interruption, a point on wave interruption generator is needed. Portable power disturbance generators are available commercially, but are expensive and are limited to 200A continuous, 600-700A peak and 480V and below [30][31]. Other non-commercial designs are available in the literature, but can not be used the motor testing in this study because of insufficient ratings or operation specifications [32][33][34][35][36]. A simple electromagnetic contactor can not be used because of the inconsistent drop-out and pick-up time. A custom built solid state device is therefore needed. This section presents the basic design of the custom built power disturbance generator that is capable of producing momentary service interruptions. Detailed design and operation information can be found in Appendix B.

3.1.1 Block Diagram

Figure 21 shows a block diagram of the solid-state POW controller. The bold lines represent the three-phase, 480V\textsubscript{rms} power circuit and the remaining lines represent the control circuit. The control circuit is powered by a 5V\textsubscript{dc} power supply. The actual switching of each phase of the power circuit is performed by a single fiber optic controlled Integrated Gate Commutated Thyristor (IGCT).
The IGCTs used in the power disturbance generator are each capable of interrupting up to 400A\textsubscript{rms} at 600V\textsubscript{rms}, but IGCTs with a higher rating may also be used. Additionally, IGCTs can operate in parallel, so much higher power ratings can be achieved if needed.

The switch controller is capable of point-on-wave (POW) switching in 0.01 cycle increments. The switch is therefore able to open at any one of 100 equally spaced points on a single cycle of the 60Hz supply voltage waveform, remain open for between 0.01 and 9999.99 cycles, and close at any one of 100 equally spaced points on a single cycle of the 60Hz supply voltage waveform. Since up to 400A\textsubscript{rms} must be interrupted at such a precise time, the switch design uses solid-state power electronics instead of mechanical switches.
3.1.2 Operation

The operation of the power disturbance generator is such that the user programs the three counters corresponding to the desired total interruption duration and the clocks count down the programmed values. The counters can be programmed either manually or automatically with a PC through the parallel port. The automatic feature is advantageous when multiple tests are conducted back to back.
The drop-out counter and the pick-up counter count from 0-99, which corresponds to 100 equally spaced points on the supply voltage reference waveform. Counter number zero on the drop-out or pick-up counter corresponds to the positive going zero crossing of the supply voltage reference waveform. Increasing the counter number moves the point along the supply voltage reference waveform away from the positive going zero crossing. For example, counter number 25 corresponds to the positive peak of the supply voltage reference waveform, counter number 50 corresponds to the negative going zero crossing of the supply voltage reference waveform, and counter number 75 corresponds to the negative peak of the supply voltage reference waveform, as shown in Fig 22.

![Figure 22 POW counter programming. 100 equally spaced points on a 60Hz sine wave.](image)

The duration counter counts from 0-9999. Each counter number on the duration counter represents one cycle of the supply voltage reference waveform. The interruption duration can therefore vary between 0 and 9999 cycles.
Figure 23 shows an example interruption. In this case, the drop-out counter is programmed to 75, the duration counter is programmed to 2, and the pick-up counter is programmed to 25. The user can therefore control the point on the 60Hz. voltage waveform that the interruption begins, how long the interruption lasts, and the point on the 60Hz. voltage waveform that the interruption ends.

For both voltage sags and momentary service interruptions, the user can select between a single phase event, a two phase event, or a three phase event. For a single phase event, any of the three phases can be selected. For a two-phase event, any two phases can be selected.

Figure 23 Example momentary service interruption. Drop-out at negative voltage peak, 2 cycle duration, pick-up at positive voltage peak.
3.2 PRELIMINARY EXPERIMENTAL WORK

Initial experimental work was conducted on a 208/220/460 V, 4 pole, 10 hp squirrel cage induction motor. The motor was connected to a 208 V supply and results from these tests are presented first.

3.2.1 Mechanical Configuration

The experimental data for the preliminary testing is acquired from the mechanical configuration shown in Fig 24. A three-phase TEFC 10 hp GE squirrel cage induction motor is used as the test motor and a Clayton Industries power absorption unit is used as the load. In order to capture shaft torque, the induction motor and power absorption unit are connected through a 5000 in-lb Lebow torque transducer. Torsionally stiff couplings are used to ensure high frequency torque transients are not damped out by coupling play. Since these “single-flex” type couplings are used, the torque transducer can not be foot mounted and must be free floated. A 1024 point per revolution speed encoder is attached to the torque transducer to acquire shaft speed. To minimize the possibility of torque ripple due to shaft misalignment, a laser shaft alignment tool is used to align the 10 hp induction motor to the power absorption unit. The load inertial is varied by attaching a flywheel to the shaft of the power absorption unit. This allows test results for two different load inertias.
3.2.2 Electrical Configuration

Fig 25 shows the electrical configuration for the 10 hp preliminary tests. The three phase variacs allow the motor voltage to be precisely controlled. In this case, 208V is supplied to the motor and the per-cent unbalance between any two phases is kept below 1%. The variacs are connected to the primary side of an isolation transformer. The secondary side is connected to a fused disconnect. The fused disconnect is connected to a NEMA 5 motor starter, which is connected to the power disturbance generator and the power disturbance generator is connected to the induction motor. The NEMA 5 starter is only used to energize
the power disturbance generator and remains closed during the tests. The power disturbance generator controls the motor.

Current waveforms are captured using hall effect current transformers placed between the power disturbance generator and the induction motor. Motor voltage waveforms are captured from sense leads placed at the motor terminals. The motor voltage, current, torque, and speed are all acquired with a 12 channel Nicolet transient analyzer operating at a 1 MHz sampling rate.

3.2.3 Experimental Test Plan

Momentary service interruption data is acquired for two different load inertias and four different motor loads, as described in Table 1. Data is collected every 0.10 of a cycle. Load inertia “A” is approximately equal to half the inertia of the 10 hp induction motor rotor. Inertial “B” is approximately equal to twice the rotor inertia.

<table>
<thead>
<tr>
<th>Table 1 Experimental tests conducted</th>
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<tbody>
<tr>
<td>Inertia “A”</td>
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<tr>
<td>-------------</td>
</tr>
<tr>
<td>Inertia “B”</td>
</tr>
</tbody>
</table>

Figure 25 Electrical test configuration
3.2.4 Experimental Test Results

3.2.4.1 Worst Case Shaft Torque

Figure 26 shows the worst-case shaft torque transient found from all of the tests listed in Table 1. This transient occurs when the interruption duration is 5.5 cycles and load inertia “B” is used at 100% load. The top graph shows the motor terminal voltage, the second graph shows the motor current, the third graph shows the shaft torque, and the fourth graph shows the shaft speed. All four graphs are plotted as a function of time. Before time t=0, the motor is operating at steady state condition. At time t=0, the motor supply voltage is removed. 5.5 cycles later, the supply voltage is reapplied. At the instant the supply voltage is reapplied, the back-emf from the motor is approximately 194 degrees out of phase with the supply voltage. Before the motor rotor reaccelerates, a sudden deceleration occurs resulting in a large negative shaft torque transient of –7300 in-lb. The rated torque for the 10 hp induction motor is 365 in-lb. Therefore, in this case, the negative torque peak is 20 times greater than the rated torque.

The results shown in Fig 26 agree with the behavior of the experimental studies shown in [8] and [9]. However, the magnitude of the torque peak in Fig 26 is much greater. [8] shows a worst case torque peak of 5.5 per unit and [9] shows a worst case torque peak of 9.5 per unit. Figure 26 shows a 20 per unit torque peak. This difference is possibly due to the different load inertias used and indicates that the effect of load inertia should be studied further.
3.2.4.2 Shaft Torque and Stator Current Magnitude as a Function of Interruption Duration

For the remaining data collected in Table 1, the magnitude of the negative torque peak and the magnitude of the current peak is determined for each interruption duration. These peaks are plotted a function of the interruption time in Fig 27 for the 100% load case.
Figure 27 Peak torque and current magnitudes as a function of interruption duration

Figure 27 shows negative torque peaks and valleys. Likewise, current peaks and valleys are shown. The negative torque peaks correspond to interruption times that require the supply voltage to be reapplied when it is out of phase with the back-emf of the motor. The negative torque valleys correspond to interruption times that require the supply voltage to be reapplied when it is in phase with the back-emf of the motor. If the back-emf of the motor and the supply voltage are in phase at the moment of reconnection, no negative torque transient is generated. If the back-emf of the motor and the supply voltage are out of phase at the moment of reconnection, a large negative torque peak is generated.

The load inertia obviously has a great influence on the magnitude of the negative torque peaks. The larger load inertia clearly produces more severe negative torque peaks. The worst case negative torque peak for load inertia “A” occurs at 3.9 cycles. The worst case negative torque peak for load inertia “B” occurs at 5.5 cycles, which is the transient shown in Fig 26. When the back-emf of the motor and the supply voltage are in phase, no negative torque peak occurs. This scenario occurs first at 6 cycles for load inertia “A” and at 8.5 cycles for load inertia “B”. The torque peaks occur at a longer interruption time for the
larger load inertia. This is due to the larger load inertia preventing the shaft speed from slowing as quickly and thus keeping the back-emf of the motor and the supply voltage in phase for a longer period of time. Although the torque peaks are much greater for the larger inertia case, the magnitude of the current peaks are approximately the same for both load inertias.

3.2.4.3 Shaft Torque and Stator Current Magnitude as a Function of Motor Loading

Figure 28 shows the worst case torque magnitude and the worst case current magnitude found for both load inertias at motor loadings described in Table 1. The results are plotted in per-unit. Clearly the load has little effect on the peak torque and peak current magnitude. On the other hand, the load did have a significant effect on the interruption duration at which the worst case current and torque transients occur. Decreasing the load increases the interruption duration at which the worst case torque and current transients occur. Figure 28 also clearly shows that the worst case current transient does not occur at the same interruption duration that the worst case torque transient occurs.

Figure 28 Worst case peak torques and peak currents for various motor loadings
3.3 COMPREHENSIVE EXPERIMENTAL RESULTS

The most significant results from the preliminary testing are that the motor loading does not effect the magnitude of the worst case current and shaft torque transients and that the load inertia does have a significant effect on the magnitude of the worst case current and shaft torque transients. Based on these findings, the comprehensive testing will include three different load inertias for each of the three motors tested and all of the tests will be conducted at 100% load.

3.3.1 Electrical Configuration

The electrical test configuration for the comprehensive testing is similar to the electrical test configuration for the preliminary testing shown in Fig 29. 480V\text{rms} three-phase power is fed to three variacs, which are used for voltage control. The variacs can be adjusted independently from 0 – 600V\text{rms} and are used to balance the three-phase supply voltage. An isolation transformer is used to isolate the power system ground, but the impedance of the variacs and the transformer is not large enough to affect the transient current magnitude. The fused disconnect and the NEMA 5 starter is used only for fault current protection, overload protection of the motor, and a disconnecting means. Control of the motor during testing is achieved by the power disturbance generator, which is necessary to precisely vary the momentary service interruption duration.

3.3.2 Mechanical Configuration

Figure 29 shows the mechanical test configuration. Torsionally stiff couplings are used on all shaft couplings in order to minimize shaft torque damping. A 1024 pulse-per-revolution (ppr) speed encoder is fixed to the torque transducer to capture shaft speed. The motor is coupled to a free-floating torque transducer, which is coupled to a custom built variable inertia flywheel device. The variable inertia flywheel device is used to adjust the load inertia and is coupled to a water brake power absorption unit. The torque of the power absorption unit varies approximately as the square of the speed.

The variable inertia flywheel device consists of a shaft-hub assembly that allows for fastening two flywheels to a hub. When the Flywheels 1 and 2 are parked in the positions shown in Fig 29, they are not attached to the shaft or hub and do not contribute to the load
inertia. However, if either Flywheel 1, Flywheel 2, or both flywheels are fastened to the hub, the load inertia is changed. This allows three different load inertias for each motor tested. The hub/shaft assembly alone results in Inertia 1, Flywheel 1 fastened to the hub/shaft assembly results in Inertia 2, and Flywheels 1 and 2 fastened to the hub/shaft assembly results in Inertia 3. The flywheels and the hub are designed such that each of the three inertias represents a common load inertia found in industry, as reported in NEMA MG1 [23]. Inertia 1 represents a centrifugal pump, Inertia 2 represents a reciprocating compressor, and Inertia 3 represents a centrifugal fan.

In order to minimize torque reading errors from the torque transducer, the entire system is aligned with a laser shaft alignment tool. The flywheels are designed to be easily added and removed from the system since realigning the motor, variable inertia flywheel device, and load is time consuming.
3.3.3 Data Acquisition System

The data acquisition system is capable of simultaneously acquiring three-phase motor voltage, three-phase supply voltage, three-phase current, shaft torque, and shaft speed, as shown in Fig 30. A Nicolet Multi-Pro Transient Analyzer is used for all of the data acquisition measurements. In order to capture any high frequency torque, speed, current, or voltage components, the system is capable of sampling at 1MHz. However, during the preliminary testing, it was determined that only the speed encoder needs to be sampled at
1MHz. The current and torque, which shared an input card, were sampled at 100kHz, the supply voltage was sampled at 10kHz, and the motor voltage, which shared an input card with the speed encoder, was sampled at 1MHz.

The motor voltages and the supply voltages were measured by connecting voltage probes directly to the lines. The current was measured by converting the output of Hall-Effect current transducers to a voltage using a precision resistor. This voltage was measured with a voltage probe and later converted to current. The output from the torque transducer was first amplified with a precision instrumentation amplifier and then measured with a voltage probe. This voltage was later converted to torque. The torque transducer and the amplifier circuit were calibrated using NIST traceable calibrated weights. The output voltage of the speed encoder was directly measured and the resulting pulse train was later converted to shaft speed.

Figure 30 Data acquisition system for comprehensive testing
3.3.4 Experimental Test Plan

The experimental test plan for the comprehensive testing is similar to that of the preliminary testing. However in this case, the interruption duration is varied from 1 to 99 cycles and three different load inertias are used for each of three different motors. Table 2 lists the tests.

<table>
<thead>
<tr>
<th>Motor</th>
<th>Load Inertia</th>
<th>Duration</th>
<th>Loading</th>
</tr>
</thead>
<tbody>
<tr>
<td>Inertia 1</td>
<td>1-99 cycles</td>
<td>100%</td>
<td></td>
</tr>
<tr>
<td><strong>10 hp</strong></td>
<td>Inertia 2</td>
<td>1-99 cycles</td>
<td>100%</td>
</tr>
<tr>
<td>Inertia 3</td>
<td>1-99 cycles</td>
<td>100%</td>
<td></td>
</tr>
<tr>
<td>Inertia 1</td>
<td>1-99 cycles</td>
<td>100%</td>
<td></td>
</tr>
<tr>
<td><strong>50 hp</strong></td>
<td>Inertia 2</td>
<td>1-99 cycles</td>
<td>100%</td>
</tr>
<tr>
<td>Inertia 3</td>
<td>1-99 cycles</td>
<td>100%</td>
<td></td>
</tr>
<tr>
<td>Inertia 1</td>
<td>1-99 cycles</td>
<td>100%</td>
<td></td>
</tr>
<tr>
<td><strong>75 hp</strong></td>
<td>Inertia 2</td>
<td>1-99 cycles</td>
<td>100%</td>
</tr>
<tr>
<td>Inertia 3</td>
<td>1-99 cycles</td>
<td>100%</td>
<td></td>
</tr>
</tbody>
</table>

3.3.5 Experimental Test Results

3.3.5.1  Magnitude of Motor Quantities as a Function of Interruption Duration

As with the preliminary testing, for each interruption duration the absolute value of the peak current, I, the maximum shaft torque, the minimum shaft torque, and the minimum speed, are recorded. These values are plotted in Fig 31 as a function of the interruption duration for the 10 hp Inertia 2 case. As expected, the results are similar to the preliminary testing results. The major difference is that the worst case shaft torque transient now occurs at 7.3 cycles and the magnitude is greater than the magnitude of the shaft torque transient measured during the preliminary testing. This is expected since the load inertia is greater.
The magnitude of the torque transient is greatest when the back-emf from the motor is about 200 degrees out of phase with the supply voltage when the supply voltage returns. Also, the magnitude of the torque transient decreases as the magnitude of the back-emf decreases. Fig 32 illustrates this behavior. The maximum torque, the minimum torque, the phase angle between the back-emf of the motor and the supply voltage at the instant of reconnection, and the per-unit magnitude of the back-emf at the instant of reconnection are plotted. Clearly there is no negative torque transient present when the back-emf of the motor and the supply voltage are in phase at the instant of reconnection.

Figure 31 Maximum shaft torque, minimum shaft torque, current peak, shaft speed versus interruption duration
3.3.5.2 Magnitude of Shaft Torque for Different Load Inertias

As indicted in Table 2 the tests presented above are repeated using two other inertia options in addition to the inertia options used in the preliminary testing. These inertia values are listed in Table 3 of the following section.

Fig. 33 shows the first negative torque peak seen in Figs 27 and 31 for the first three 10 hp load inertias listed in Table 3 of the following section. Clearly the load inertia has a dramatic affect on the magnitude of the worst case torque transient and the interruption duration at which the worst case will occur.
Figure 33 Torque transient magnitude vs interruption duration for three load inertias

3.3.5.3 Magnitude of Shaft Torque and Stator Current During Worst Case and During Starting

Additional tests are carried out on a 50 hp and a 75 hp motor with three different load inertias. The magnitude of the worst case torque transient and the magnitude of the worst case current transient for each of these tests are included in Table 3. Measured starting current and starting torques for each set up is also included as a reference for the torque and current interruption transient magnitudes. The base of the per-unit current quantities is the measured steady state peak current and the base of the per-unit torque quantities is the measured steady state full load torque.
Table 3 shows that the magnitude of the worst case torque transient is proportional to the size of the load inertia. As the load inertial size increases, so does the magnitude of the worst case torque transient. In the case of the 75 hp motor with load inertia 75-3, the magnitude of the torque transient reached a value of 32 times the measured steady state full load torque and over 5 times the measured starting torque.

### Table 3  Peak measured torque and current due to starting and due to a momentary service interruption

<table>
<thead>
<tr>
<th>Motor</th>
<th>Motor Rotor Inertia</th>
<th>Load Inertia Name</th>
<th>Load Inertia Value</th>
<th>Peak Starting Current (A) / (pu)</th>
<th>Peak Interruption Current (A) / (pu)</th>
<th>Peak Starting Torque (in-lb) / (pu)</th>
<th>Peak Interruption Torque (in-lb) / (pu)</th>
</tr>
</thead>
<tbody>
<tr>
<td>10hp</td>
<td>0.0589 kgm²</td>
<td>Inertia 10-A</td>
<td>0.02989 kgm²</td>
<td>169 / 11</td>
<td>281 / 19</td>
<td>1378 / 4</td>
<td>-3747 / 10</td>
</tr>
<tr>
<td></td>
<td></td>
<td>Inertia 10-B</td>
<td>0.1389 kgm²</td>
<td>165 / 11</td>
<td>273 / 18</td>
<td>1998 / 6</td>
<td>-7322 / 20</td>
</tr>
<tr>
<td></td>
<td></td>
<td>Inertia 10-1</td>
<td>0.1489 kgm²</td>
<td>169 / 11</td>
<td>280 / 19</td>
<td>4531 / 13</td>
<td>-9369 / 26</td>
</tr>
<tr>
<td></td>
<td></td>
<td>Inertia 10-2</td>
<td>0.2521 kgm²</td>
<td>164 / 11</td>
<td>274 / 18</td>
<td>5587 / 16</td>
<td>-9594 / 27</td>
</tr>
<tr>
<td></td>
<td></td>
<td>Inertia 10-3</td>
<td>0.4583 kgm²</td>
<td>166 / 11</td>
<td>269 / 18</td>
<td>5812 / 16</td>
<td>-9535 / 26</td>
</tr>
<tr>
<td>50hp</td>
<td>0.3889 kgm²</td>
<td>Inertia 50-1</td>
<td>0.7665 kgm²</td>
<td>825 / 10</td>
<td>1507 / 19</td>
<td>7101 / 4</td>
<td>-35412 / 20</td>
</tr>
<tr>
<td></td>
<td></td>
<td>Inertia 50-2</td>
<td>1.4271 kgm²</td>
<td>840 / 11</td>
<td>1541 / 20</td>
<td>8189 / 5</td>
<td>-41970 / 24</td>
</tr>
<tr>
<td></td>
<td></td>
<td>Inertia 50-3</td>
<td>2.6988 kgm²</td>
<td>859 / 11</td>
<td>1507 / 19</td>
<td>10204 / 6</td>
<td>-45553 / 26</td>
</tr>
<tr>
<td>75hp</td>
<td>0.6685 kgm²</td>
<td>Inertia 75-1</td>
<td>1.1611 kgm²</td>
<td>1292 / 11</td>
<td>2226 / 19</td>
<td>7165 / 3</td>
<td>-73287 / 28</td>
</tr>
<tr>
<td></td>
<td></td>
<td>Inertia 75-2</td>
<td>2.2083 kgm²</td>
<td>1251 / 11</td>
<td>2184 / 19</td>
<td>9436 / 4</td>
<td>-81988 / 31</td>
</tr>
<tr>
<td></td>
<td></td>
<td>Inertia 75-3</td>
<td>4.3051 kgm²</td>
<td>1296 / 11</td>
<td>2203 / 19</td>
<td>15514 / 6</td>
<td>-85539 / 32</td>
</tr>
</tbody>
</table>

### 3.4 STRESS ASSESSMENT

Comparing the magnitude of the starting currents to the magnitude of the interruption currents shows that the interruption currents are nearly twice that of the starting currents in all cases. Although the magnitude of the interruption current is greater, the duration of this current is much shorter than the duration of starting currents. This is due to the fact that the motor rotor is already spinning at the instant the supply voltage returns from the interruption. Once the back-emf and the supply voltage are realigned, acceleration to full speed doesn’t require as much time as accelerating the rotor from standstill. In comparing the length of the starting current transients to the length of the momentary service interruption transients, it is noted that on average, the starting transients last 10 times longer than the momentary service interruption transients. The heating effect of the interruption currents are therefore less than the heating effects of the startup currents and should not cause any adverse problems. [12] reports this finding as well.
As mentioned in section 2.1, the most common area of failure due to excessive mechanical stresses in an induction motor is the coupling keyway area of the shaft [6]. Calculating the shaft torque necessary to exceed the yield strength of key can be made using (2.1), re-written here for convenience.

\[ \text{Torque (in-lb)} = \frac{\text{YieldStrength (psi)} \times \text{ShaftRadius (in)} \times \text{KeyWidth (in)} \times \text{KeyLength (in)}}{12} \]

Table 4 shows the calculated torque required to yield the shaft key and the worst case measured torque magnitude for the 10 hp, 50 hp, and 75 hp test motors. The yield strength of the key steel is assumed to be 45000psi, as reported in [6]. The results reported in Table 4 show that even the worst-case scenario does not produce enough torque to yield the shaft key. Furthermore, according to the behavior predicted in Fig 34 below, the worst case torque peak for any given load inertia will not reach the torque required to yield the shaft key.

<table>
<thead>
<tr>
<th>Motor</th>
<th>Shaft Key Yield Torque (in-lb)</th>
<th>Worst Case Measured Torque (in-lb)</th>
</tr>
</thead>
<tbody>
<tr>
<td>10 hp</td>
<td>24,170</td>
<td>9,594</td>
</tr>
<tr>
<td>50 hp</td>
<td>89,648</td>
<td>67,767</td>
</tr>
<tr>
<td>75 hp</td>
<td>125,244</td>
<td>85,527</td>
</tr>
</tbody>
</table>

From the experimental data gathered, the load inertia appears to be the main factor that determines the magnitude of the worst case torque transient. Figure 34 plots the magnitude of the worst case torque transients for each motor as a function of load inertia. These points are fitted to an exponential curve that predicts the magnitude of the worst case torque transient for all load inertias. The curve takes the form of

\[ y(x) = -A + Be^{(-Cx)} \]

Figure 34 shows that the magnitude of the worst-case torque transient asymptotically approaches a limit with increasing load inertia. Due to the exponential nature of the curve, there is a point at which further increasing the load inertia does not significantly increase the magnitude of the worst-case torque transient.
Experimentally, the calculated values in Table 4 appear to be reasonable, as no keys were damaged during any of the testing. However, slight shaft deformation occurred adjacent to the shaft keyway on the 50 hp and 75 hp motors. But this deformation did not inhibit the operation of the motor and was likely due to repeated successive testing and not from one single momentary service interruption. Each motor was subjected to over 2,000 momentary service interruptions. The 10 hp motor was subjected to over 9,000 momentary service interruptions and no damage was noted. An IEEE 112-B test was conducted before and after the testing and no adverse effects were noted on the motor.
Chapter 4

Simulation Based Stress Assessment of Single Motors

4.1 STANDARD MOTOR MODEL

In order to aid in explaining the results from the experimental data and to extend the study conditions not easily tested in the laboratory, a model of the system constructed in the laboratory is needed. A good starting point is the standard dq0 reference frame induction motor model introduced in the prior work section.

The familiar equations of the standard induction motor model represented in the dq0 stationary reference frame are as follows [37][38]. A more in-depth treatment is covered in Appendix A.

The stator voltages are:

\[
v^{s}_{ds} = r^{s}_{s}i^{s}_{ds} + \frac{d\lambda^{s}_{ds}}{dt} \tag{4.1a}
\]

\[
v^{s}_{qs} = r^{s}_{s}i^{s}_{qs} + \frac{d\lambda^{s}_{qs}}{dt} \tag{4.1b}
\]

\[
v^{s}_{0s} = r^{s}_{s}i^{s}_{0s} + \frac{d\lambda^{s}_{0s}}{dt} \tag{4.1c}
\]

The rotor voltages are:

\[
v^{s}_{qr} = r^{s}_{r}i^{s}_{qr} + \frac{d\lambda^{s}_{qr}}{dt} - \omega^{s}_{r}\lambda^{s}_{dr} \tag{4.2a}
\]

\[
v^{s}_{dr} = r^{s}_{r}i^{s}_{dr} + \frac{d\lambda^{s}_{dr}}{dt} - \omega^{s}_{r}\lambda^{s}_{qr} \tag{4.2b}
\]

\[
v^{s}_{0r} = r^{s}_{r}i^{s}_{0r} + \frac{d\lambda^{s}_{0r}}{dt} \tag{4.2c}
\]
And the flux linkages are:

\[
\begin{bmatrix}
\lambda_{qr}^s \\
\lambda_{ds}^s \\
\lambda_{br}^s \\
\lambda_{qr}^c \\
\lambda_{ds}^c \\
\lambda_{br}^c
\end{bmatrix} =
\begin{bmatrix}
x_b + x_m & 0 & 0 & x_m & 0 & 0 \\
0 & x_b + x_m & 0 & 0 & x_m & 0 \\
0 & 0 & x_b & 0 & 0 & 0 \\
0 & 0 & 0 & x_b + x_m & 0 & 0 \\
0 & 0 & 0 & x_b + x_m & 0 & 0 \\
0 & 0 & 0 & 0 & 0 & x_b
\end{bmatrix}
\begin{bmatrix}
i_{qr}^s \\
i_{ds}^s \\
i_{br}^s \\
i_{qr}^c \\
i_{ds}^c \\
i_{br}^c
\end{bmatrix}
\]

where \( \omega_b = 2 \pi f_{\text{rated}} \) and \( f_{\text{rated}} \) is the power supply system frequency.

The electromagnetic torque developed by the induction motor is expressed as:

\[
T_{em} = \frac{3}{2} P \left( \lambda_{ds}^s i_{qs}^s - \lambda_{qs}^s i_{ds}^s \right)
\]

(4.4)

where \( P \) is the number of poles.

The standard load model assumes a single inertia for the rotor and load and hence the rotor dynamics become

\[
J \frac{d\omega}{dt} + T_{\text{mech}} + T_{\text{damp}} = T_{em}
\]

(4.5)

Eq (4.5) is the equation of motion of the rotor with torques \( T_{em}, T_{\text{mech}}, \) and \( T_{\text{damp}} \) acting on a single inertia, \( J \). \( T_{\text{mech}} \) is the mechanical torque produced by the load and \( T_{\text{damp}} \) is the torque produced due to friction and windage. Inertia \( J \) is the sum of all inertias connected to the motor shaft and includes the rotor inertia, couplings, and load inertia.

**4.2 NON-LINEAR MOTOR MODEL**

The standard motor model neglects the following factors: line impedance, stator leakage reactance saturation, rotor resistance variations with slip, and rotor leakage reactance variations with slip, mutual flux path saturation, and stray load losses. These factors are often critical in induction motor behavior during large transient such as start-up and therefore need to be taken into account.
4.2.1 Line Impedance

To account for the impedance in the supply line feeding the motor, a series resistance and inductance is added between an ideal source and the motor. The line impedance is therefore represented by a series R-L circuit, as shown in Fig 35. It is assumed that the resistance and inductance of each phase is equal and that the ideal source voltage is a balanced 480 V source.

![Diagram of the ideal source, line impedance model, and motor](image)

Figure 35 Diagram of the ideal source, line impedance model, and motor

4.2.2 Stator Leakage Reactance Saturation

During transient conditions, the performance of the motor can be dominated by leakage reactance saturation if the magnitudes of the current transients are high [39]. There are methods of approximating the saturation effects if the dimensions of the stator slots are known. However, in this case, the motor slot dimensions are not known so a simplified approach is taken. The stator leakage reactance saturation varies with the stator current. A typical plot of this variation is shown in Fig 36 [39].
Although the absolute values of \( x_{ls_{\text{typical}}} \) in Fig 36 are probably incorrect for the motors presented in the experimental data section, but the shape of the curve is most likely correct. Therefore the actual values can be estimated by using an initial value plus a scaled factor of the typical values. This effectively adjusts the curve in Fig 36 by an offset and a gain.

\[
x_{ls} = x_{ls0} + D_{slr}x_{ls_{\text{typical}}}
\]

(4.6)

The stator current per-unit quantity is obtained from the motor model by dividing the real-time RMS value of the stator current by the rated current of the induction motor.

### 4.2.3 Rotor Resistance Variations with Slip

The slip in an induction motor describes the difference between the rotating MMF field due to the stator currents and the rotating MMF field due to the mechanical rotor rotation. Per-unit slip is therefore defined as [38]

\[
s = \frac{\omega_e - \omega_r}{\omega_e}
\]

(4.7)

where \( \omega_e \) is the angular speed of the rotating stator MMF field and \( \omega_r \) is the mechanical rotor speed.
At the instant a motor starts the slip is 1, as the mechanical rotor speed is zero. When the motor reaches rated speed, the slip decreases to a value on the order of 0.01.

The frequency of the rotor currents are found by

\[ f_r = s f_e \]  \hspace{1cm} (4.9)

where \( f_e \) is the frequency of the rotor currents and \( f_s \) is the power supply system frequency.

As an induction motor starts, the rotor currents vary significantly. At a rotor speed of zero, the rotor currents are near the stator current frequency, 60 Hz, in the US. At full speed, the rotor currents are on the order of 0.6 Hz. This frequency variation causes variations in the rotor resistance and rotor reactance parameters.

Rotor resistance variations occur primarily because of skin and proximity effect [39]. The equivalent resistance due to skin effect in a round conductor is known to vary as the square root of the frequency [40]

\[ R_{eq} = C \sqrt{f} \]  \hspace{1cm} (4.10)

where \( f \) is frequency and \( C \) is a constant that is a function of the conductor properties and the conductor dimensions.

Resistance variations in the rotor can be accounted for by varying the rotor resistance as described by (4.10), where the frequency is simply derived from the slip

\[ r_r = r_{r0} + C_{rr} \sqrt{s} \]  \hspace{1cm} (4.11)

4.2.4 **Rotor Leakage Reactance Variation with Slip**

The rotor leakage reactance variation is also due to frequency variations in the rotor currents. However, leakage reactance decreases with increasing frequency whereas resistance increases with increasing frequency [40]. The leakage reactance variation is therefore expressed as
\[ x_{lr} = x_{lr0} + \frac{C_{Slr}}{\sqrt{s}} \] (4.12)

which produces a curve similar to the typical rotor leakage reactance variation shown in [39].

### 4.2.5 Mutual Flux Path Saturation

The standard motor model described by equations (4.1) – (4.3) assumes a linear current-flux linkage relationship in the mutual flux path, as described by the air gap line in Fig 37. In reality, the magnetic material of the motor can saturate with increasing current, which produces a current-flux linkage relationship described by the saturation curve. To incorporate this into the motor model, mutual flux path saturation is modeled by updating the flux linkage quantities in equations (4.1) – (4.3) using a lookup table. The lookup table is derived by finding the difference between the air gap line of the \( \lambda \)-i curve and the saturation curve and plotting this difference vs. \( \lambda \), as shown in Fig 38 [38].

![Figure 37 Typical air gap line and saturation curve for magnetic material commonly found in induction motors](image-url)
The saturation curve shown in Fig 37 is obtained through no load tests of the motor, as described in [38]. A no load stator voltage vs. stator current graph is generated and the stator voltage is converted to flux linkage.

Mutual flux path saturation is often included in induction motor models [38][41], however, [39] claims that the majority of the saturation occurs in the leakage reactance terms and rarely extends to the major parts of the machine. Simulations including and omitting mutual flux path saturation are shown in the results section.

**4.2.6 Stray Load Losses**

Stray load losses can affect the torque-speed characteristics of an induction motor. Examples of the primary sources of stray load losses are friction, windage, and core losses [39]. Since friction and windage are accounted for in the load model, the only other stray load losses that should be addressed in the motor are the core losses of the machine. The core losses cause a loss in the electromagnetic torque, $T_{em}$, developed by the machine. However, the magnitude of the loss is on the order of 0.25 per-cent of the developed torque [39] and is therefore too small significantly contribute to the results. For this reason, core losses will remain neglected.
4.2.7 Dynamic Load Model

Motor behavior with a dynamic mechanical load is quite different than that of an unloaded or constant torque case. In order to accurately model an induction motor behavior, we need to represent not only the induction motor, but also the mechanical load. This also includes a torque transducer model, so that the measured torque can be compared to the simulated torque.

A diagram of the motor/load system constructed in the laboratory is shown in Fig 39. A model representing the motor/load system is also shown. The motor rotor and motor couplings are lumped into an equivalent inertia called $J_m$. The load couplings, the load, and the variable inertia flywheel device are lumped into an equivalent inertia called $J_l$. The torque transducer is modeled as a spring/dampener device with spring constant $k_{TT}$ and damping coefficient $C_{cTT}$. The motor bearings, $R_{bm}$, and the load bearings, $R_{bl}$ are modeled by a constant torque opposing the direction of rotation. Friction and windage losses of the motor are modeled by a viscous damping coefficient $C_{t_m}$. Likewise, friction and windage losses of the load are modeled by a viscous damping coefficient $C_{t_l}$.

The electromagnetic torque produced by the induction motor is called $T_{em}$ and the opposing torque produced by the water brake load is called $T_L$. 

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The equations of motion for the system represented in Fig 4 can be expressed as [39][41]

\begin{align}
J_m \ddot{\theta}_m &= T_{em} - C_{tm} \dot{\theta}_m - C_{ml} (\dot{\theta}_m - \dot{\theta}_L) - k_{rT} (\theta_m - \theta_L) - R_{bm} \\
J_L \ddot{\theta}_L &= -T_L - C_{tl} \dot{\theta}_L + C_{ml} (\dot{\theta}_m - \dot{\theta}_L) + k_{rT} (\theta_m - \theta_L) - R_{bl}
\end{align}

(4.13a) (4.13b)

According to the literature provided with the load, the load torque \( T_L \) varies as the square of the speed.

\[ T_L = D\dot{\theta}_L^2 \]

(4.14)

where \( D \) is a constant that depends on the load setting of the load.
The torque transducer is a strain gauge shaft assembly which measures shaft deflection. The strain gauges are fastened onto the shaft and electrically connected into a Wheatstone Bridge configuration. As torque is applied to the shaft, the output voltage of the Wheatstone bridge increases. This output voltage is proportional to the applied torque.

A shaft deflection multiplied by a spring constant is equal to torque [42]. Therefore, the shaft torque measured from the torque transducer is

$$T_s = k_{TT} (\theta_m - \theta_L)$$

(4.15)

The shaft speed, which is measured at the load, is

$$\omega = \dot{\theta}_L$$

(4.16)

### 4.3 PARAMETER ESTIMATION

In order to perform simulations using the models presented above, several parameters must be known. Table 5 lists the parameters that are needed for each model.

<table>
<thead>
<tr>
<th>Table 5 Parameters needed for the motor system model</th>
</tr>
</thead>
<tbody>
<tr>
<td><strong>Model</strong></td>
</tr>
<tr>
<td>Line Impedance Model</td>
</tr>
<tr>
<td>Standard Motor Model</td>
</tr>
<tr>
<td>Non-Linear Model</td>
</tr>
<tr>
<td>Dynamic Load Model</td>
</tr>
</tbody>
</table>

#### 4.3.1 Parameter Estimation Method

The parameter estimation method used to determine the parameters listed in Table 5 is a non-linear, weighted least squares method. In general, the least squares method minimizes the sum of the squares of the residuals, $s$, between the measured values, $z^m_i$, and the calculated values, $z^c_i$ [43][44].

$$s = \sum_{i=1}^{n} \left( z^m_i - z^c_i \right)^2$$

(4.17)

where $n =$ the number of sample points from the measured data.
The measured data is obtained from laboratory measurements. The calculated data is obtained from the equations that describe the system.

In matrix form, the set of measured data, calculated data, and parameters can be expressed as

\[ \underline{y} = h(\delta) \]  

(4.18)

where \( \underline{y} \) is the calculated values and \( \delta \) is the parameters values.

Non-linear least square methods estimate parameters \( \hat{\delta} \) such that the residuals \( z_i^m \) and \( z_i^c \) are minimized. Usually numerical methods use (4.18) and it’s gradient for this purpose\[43]\[44]. However, the method used in this study is the parameter identification toolbox provided by Matlab.\[45]\ The method employs a general search method which does not require explicit gradient of (4.18).

The following sections describe the use of this method to estimate the parameters listed in Table 5.

### 4.3.2 Line Impedance Model Parameters

The motor current and the motor voltage are known from experimental measurements. The motor current is equal to the current flowing through the line. Using this data, a parameter estimation can be performed to determine the resistance and inductance values of the line impedance. Voltage and current data from a motor start is used, as this data provides a significant change in the motor terminal voltage due to the high starting currents required by the motor. Figure 40 shows a block diagram of the line impedance parameter estimation process. The step by step process is as follows:

1) Obtain supply voltage data from a motor start and determine the supply voltage frequency, \( f_{\text{rated}} \)
2) Obtain measured motor voltage data, \( v_{ab}, v_{bc}, \) and \( v_{ca} \), and stator current data, \( i_s, i_{bs}, i_{cs} \), from a motor start.
3) Filter all measured data using a binomial filtering algorithm \[46]\.
4) Set the frequency of the three phase ideal source to match the frequency found in step 1.
5) Adjust phase angle of the three phase ideal source so that the motor model starts at the same point on wave that the measured data starts.
6) Assign R and L an initial guess.
7) Calculate the voltage drop across the R-L circuit using the measured currents $i_a$, $i_b$, $i_c$.
8) Subtract the voltage drop (from step 7) from the ideal source and compare it to the measured motor voltage from step 3. Obtain residual error.
9) Compare the residual error to the minimum allowable residual error. If the residual error is greater than the minimum allowable residual error, proceed to step 10. If the residual error is less than the minimum allowable residual error, terminate.
10) Update parameters and return to step 7.

Parameter Estimation

$$R$$ $$L$$

Figure 40 Block diagram of line impedance parameter estimation process

4.3.3 Dynamic Load Model Parameters

The shaft torque and the shaft speed are known from laboratory measurements. Inertias $J_m$ and $J_L$ are also known quantities found by measuring the inertia of each component in the system. The inertia is measured using a torsion pendulum. The component being measured is suspended from a long wire, whose torsion constant, $c$, is known. Next, the component is forced into a twisting oscillation. The period, $T$, is measured and the inertia, $I$, is found by the following equation [47]
Table 3 in chapter 3 lists the inertia of each component in the system for each motor tested.

The remaining parameters, \( C_{tm}, C_{tL}, C_{tmL}, k_{TT}, R_{tm}, R_{tL}, \) and \( D \) can be estimated with a non-linear least squares parameter estimation technique. This technique uses the measured shaft speed data and the measured shaft torque data acquired after a motor was stopped from a loaded, steady state condition.

Once the motor is de-energized, \( T_{em} \) becomes zero and the shaft speed decreases with time until it eventually comes to a complete stop. However, once \( T_{em} \) becomes zero, the motor model does not contribute any input to the dynamic load model described by (4.13a) and (4.13b). The load dynamics are therefore de-coupled from the motor model for \( T_{em} = 0 \). This allows the load parameters to be estimated independently of the motor parameters.

A block diagram of the parameter identification process used to identify the load parameters is shown in Fig. 41. The step-by-step process is as follows:

1) Obtain measured speed data, \( \omega_r \), and shaft torque data, \( T_S \), beginning at the instant the motor is stopped from a loaded, steady state condition.
2) Filter all measured data using a binomial filtering algorithm [46].
3) Assign parameters an initial guess.
4) Assign initial condition guesses to speed and position integrals in the solution of (4.13a) and (4.13b).
5) Solve (4.13a) and (4.13b) for shaft torque, \( T_S \), and speed, \( \omega_r \).
6) Compare the solution from step 5 to the measured data from step 2 and obtain a residual error that expresses the error.
7) Compare the residual error to the minimum allowable residual error. If the residual error is greater than minimum allowable residual error, proceed to step 8. If the residual error is less than minimum allowable residual error, terminate.
8) Update parameters and initial conditions and return to step 5.
4.3.4 Standard Motor Model Parameters

The unknown parameters of the standard motor model are the stator resistance, \( r_s \), the rotor resistance, \( r_r \), the stator leakage reactance, \( x_{ls} \), the rotor leakage reactance, \( x_{lr} \), and the magnetizing reactance, \( x_m \).

For the standard motor model parameter estimation process, experimental data acquired during a motor start is used. Data from a motor start is a good data set because the speed varies from 0 to rated speed and the current varies from peak starting current to rated current. This will expose the parameter estimation process to a wide range of motor operation. The speed and stator currents are used as the measurement data. It is not necessary to use the shaft torque measurement data for the motor parameter estimation process since this data was previously used for the dynamic load model parameter estimation process.

Figure 42 shows a block diagram of the motor model parameter estimation process. The step-by-step process is as follows:

1) Obtain supply voltage data from a motor start and determine the supply voltage frequency, \( f_{\text{rated}} \).
2) Obtain measured speed data, \( \omega \), and stator current data, \( i_{as}, i_{bs}, i_{cs} \), from a motor start.
3) Filter all measured data using a binomial filtering algorithm [46].
4) Set the frequency of the three phase ideal source to match the frequency found in step 1.
5) Adjust phase angle of the three phase ideal source so that the motor model starts at the same point on wave that the measured data starts.
6) Assign motor parameters an initial guess.
7) Solve (4.1) – (4.5) and (4.13) for the speed $\omega_r$, and the stator currents, $i_{as}$, $i_{bs}$, $i_{cs}$.
8) Compare the solution of step 7 to the measured data from step 3 and obtain a residual error to express the error.
9) Compare the residual error to the minimum allowable residual error. If the residual error is greater than the minimum allowable residual error, proceed to step 10. If the residual error is less than the minimum allowable residual error, terminate.
10) Update parameters and return to step 7.

![Block diagram of the standard motor model parameter estimation process](image)

**Figure 42** Block diagram of the standard motor model parameter estimation process

### 4.3.5 Non-Linear Motor Model Parameters

In order to determine the value of the non-linear rotor terms, and the value of the modified stator leakage reactance terms, a parameter estimation is used. Fig 43 shows the block diagram of the parameter estimation process used to obtain these terms. For this parameter estimation, the line impedance is also included in the model, but the line impedance terms are not re-estimated. The parameter estimation process is similar to that of the standard motor model parameter estimation process. The only exception is the addition of the terms $D_x$, $C_{rr}$, and $C_{xlr}$ and the line impedance model.
4.3.6 Summary of Parameter Values

Table 6 lists the value of the parameters estimated for the line impedance model. The resistance values seem reasonable, as the motors tested were fed with 4/0 copper wire from a 1200 A service.

<table>
<thead>
<tr>
<th>Line Impedance Parameter</th>
<th>Estimated Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>R</td>
<td>4.27 mΩ</td>
</tr>
<tr>
<td>L</td>
<td>0.232 mH</td>
</tr>
</tbody>
</table>

Table 7 lists the estimated parameter values of the dynamic load model. The bearing friction is estimated at several orders of magnitude lower than the other parameters. This seems reasonable, as the bearings used in the variable inertia flywheel device and in the motor are high quality bearings. The motor and load rotate for a time at low speeds before coming to a complete stop.

The fact that $C_{tl}$ is estimated at over 9 times higher than $C_{tm}$ does not mean that the friction and windage of the variable inertia flywheel device is greater than that of the motor. In fact, the friction and windage of the motor are most likely greater than that of the variable inertia...
flywheel device due to the fan on the TEFC motor frame. However, the water brake load uses a rotary vane design, and this most likely contributes significantly to the $C_{tl}$ term.

Table 7 Dynamic load model estimated parameters

<table>
<thead>
<tr>
<th>Dynamic Load Model Parameters</th>
<th>Estimated Value 10 hp</th>
<th>Estimated Value 50 hp</th>
<th>Estimated Value 75 hp</th>
</tr>
</thead>
<tbody>
<tr>
<td>$R_{bm}$</td>
<td>1.2 e-013</td>
<td>2.3 e-014</td>
<td>2.3 e - 014</td>
</tr>
<tr>
<td>$R_{bl}$</td>
<td>1.6 e-008</td>
<td>2.3 e-014</td>
<td>3.0 e -005</td>
</tr>
<tr>
<td>$C_{tl}$</td>
<td>0.040</td>
<td>0.095</td>
<td>0.13</td>
</tr>
<tr>
<td>$C_{tm}$</td>
<td>0.019</td>
<td>0.011</td>
<td>0.033</td>
</tr>
<tr>
<td>$k_{ITT}$</td>
<td>0.33 e005</td>
<td>2.4 e005</td>
<td>3.0 e 005</td>
</tr>
<tr>
<td>$C_{tmf}$</td>
<td>18.93</td>
<td>12.46</td>
<td>10.94</td>
</tr>
<tr>
<td>$D$</td>
<td>0.0010</td>
<td>0.0052</td>
<td>0.0080</td>
</tr>
</tbody>
</table>

The estimated values of the motor parameters for the 10 hp, 50 hp, and 75 hp motor are listed in Table 8, Table 9, and Table 10, respectively. The motor manufacturer parameters are also included for the 10 hp and 50 hp motor for comparison. The estimated parameters, on average, require an adjustment of the motor manufacturer parameters by a factor of 4.

Table 8 10 hp induction motor parameters

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Motor Manufacturer (Ω)</th>
<th>Std. Estimation (Ω)</th>
<th>NL Estimation (Ω)</th>
</tr>
</thead>
<tbody>
<tr>
<td>$r_s$</td>
<td>0.420</td>
<td>1.01</td>
<td>0.945</td>
</tr>
<tr>
<td>$x_{ls}$</td>
<td>0.300</td>
<td>0.00177</td>
<td>0.0440</td>
</tr>
<tr>
<td>$r_r$</td>
<td>0.108</td>
<td>0.708</td>
<td>0.519-0.705 *</td>
</tr>
<tr>
<td>$x_{lr}$</td>
<td>0.300</td>
<td>2.90</td>
<td>2.46-7.51 *</td>
</tr>
<tr>
<td>$x_m$</td>
<td>12.0</td>
<td>54.0</td>
<td>134</td>
</tr>
</tbody>
</table>

* see Fig 44

Table 9 50 hp induction motor parameters

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Motor Manufacturer (Ω)</th>
<th>Std. Estimation (Ω)</th>
<th>NL Estimation (Ω)</th>
</tr>
</thead>
<tbody>
<tr>
<td>$r_s$</td>
<td>0.370</td>
<td>0.145</td>
<td>0.138</td>
</tr>
<tr>
<td>$x_{ls}$</td>
<td>0.290</td>
<td>0.0509</td>
<td>0.118</td>
</tr>
<tr>
<td>$r_r$</td>
<td>0.029</td>
<td>0.107</td>
<td>0.0375 – 0.126 *</td>
</tr>
<tr>
<td>$x_{lr}$</td>
<td>0.236</td>
<td>0.842</td>
<td>0.608 – 2.17 *</td>
</tr>
<tr>
<td>$x_m$</td>
<td>7.76</td>
<td>29.4</td>
<td>19.5</td>
</tr>
</tbody>
</table>

* see Fig 45
Table 10 75 hp induction motor parameters

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Motor Manufacturer (Ω)</th>
<th>Std. Estimation (Ω)</th>
<th>NL Estimation (Ω)</th>
</tr>
</thead>
<tbody>
<tr>
<td>$r_s$</td>
<td>unknown</td>
<td>0.0886</td>
<td>0.0781</td>
</tr>
<tr>
<td>$x_{ls}$</td>
<td>unknown</td>
<td>0.0397</td>
<td>0.0378-0.0458 *</td>
</tr>
<tr>
<td>$r_r$</td>
<td>unknown</td>
<td>0.0515</td>
<td>0.0197-0.0597 *</td>
</tr>
<tr>
<td>$x_{lr}$</td>
<td>unknown</td>
<td>0.479</td>
<td>0.436-1.10 *</td>
</tr>
<tr>
<td>$x_m$</td>
<td>unknown</td>
<td>11.0</td>
<td>14.4</td>
</tr>
</tbody>
</table>

* see Fig 46

4.3.7 Non-linear Motor Model Parameters Variation During Motor Start

Fig 44 shows the variation of the parameters estimated in Section 4.3.5 during a simulation of a 10 hp motor start. Interestingly, the stator leakage reactance term $D_{xls}$, is estimated as nearly zero. This effectively holds $x_s$ constant and does not allow for $x_s$ variations. For the 50 hp case, a very slight variation is seen and for the 75 hp case, significant variation is seen. This is shown in Figs 45 and 46 and indicates that the stator leakage reactance variations are more significant for larger motors. It is also possible that the shape of $x_{s\_typical}$ is not correct for the 10 hp and the 50 hp motor and that more variations would be observed had a different curve been used. The variations in the rotor parameters are as expected for all three motors.
Figure 44 Parameter variation during 10 hp motor start
Figure 45 Parameter variation during 50 hp motor start
The effect of including mutual flux path saturation in the modified motor model is minimal. This is shown in Fig 47, which plots of the speed during a 50 hp motor start for the case of including the mutual flux path saturation and omitting the mutual flux path saturation in the
motor model. For both cases, the modified motor model and dynamic load model is used. Although not shown, the affect on torque and current is minimal also.

Figure 47 Simulation of 50 hp motor during a motor start including mutual flux path saturation effects and omitting mutual flux path saturation effects
4.4 SIMULATION RESULTS

4.4.1 Line Impedance

Fig 48 shows $i_{s\text{ measured}}$, $v_{ca\text{ measured}}$, and $v_{ca\text{ simulated}}$. $v_{ca\text{ simulated}}$ is obtained after the parameter estimation is complete and the estimated values of R and L are used in the model. For the estimation, the ideal source supply voltage is assumed to be 480V. This is most likely not correct, since the voltage sag during motor starting appears to be greater for $v_{ca\text{ simulated}}$ than it is for $v_{ca\text{ measured}}$. However, the difference between the peaks of these two waveforms is only 3-4% and should significantly not affect the motor simulations.
Figure 48 Simulated and measured current and voltage resulting from a 50 hp motor start. The simulated voltage includes the line impedance model.

4.4.2 Dynamic Load Model

Simulations using the dynamic load model are first presented immediately following a motor stop from a fully loaded, steady state condition. This allows the response of the dynamic
load model to be analyzed when $T_{cm}$ is zero and the induction motor is not contributing any input.

4.4.2.1 10 hp Motor Set-Up

Figure 49 shows the measured and simulated shaft speed for two different load inertias, $I_1$ and $I_3$, for the 10 hp motor set up. Before time $t=0$, the motor is running at a steady state, fully loaded condition. At time $t=0$, the motor is de-energized and the shaft speed begins to drop. The measured data from the curve labeled $I_1$ is used in the parameter estimation process described above. The simulated data is derived from the load model using the estimated parameters. There is excellent agreement between the simulated and the measured data, as the simulated curve lays on top of the measured curve.

In order to validate the parameters estimated for the dynamic load model, a new set of measured data must be compared to a new set of simulated data. A good validation test is to use data from a load inertia different than the load inertia used to perform the parameter estimation. A different load inertia changes the system dynamics entirely, as the speed and torque characteristics completely change. As the inertia increases, the length of time for the shaft speed to reach standstill following a motor stop increases. For the validation data, the load inertia is increased from 0.1489 kgm$^2$ ($I_1$) to 0.4583 kgm$^2$ ($I_3$), an increase by over a factor of 3. As seen in Fig 49, even with such a large increase in the load inertia, the dynamic load model still predicts the shaft speed very accurately. The measured and simulated curves for the load inertia $I_3$ case nearly lay on top of each other. The slight difference may actually be in the load inertia value and not the load model, as the inertia measurement technique neglected the torque transducer inertia, flywheel bolts, and shaft keys.

Figure 50 shows the measured and simulated output form the torque transducer during the same conditions as Fig 49 using load inertia $I_1$. The measured data exhibits more noise and some higher frequency responses that the simulated data does not exhibit. However, the fundamental frequency of oscillation immediately following the motor stop and the general trend of the measured and simulated toque waveform agree very well.
The same speed and torque curves presented for the 10 hp case are also presented for the 50 hp and 75 hp case. The results for all three motors are very similar.

Figure 49 Measured and simulated speed following a 10 hp motor stop using load inertia $I_1$ and $I_3$
Figure 50 Measured and simulated shaft torque following a 10 hp motor stop using load inertia I1

4.4.2.2  50 hp Motor Set-Up

Figure 51 and 52 shows similar data sets as Fig 49 and Fig 50, but for the 50 hp case. As with the 10 hp data, there is excellent agreement between the simulated and the measured data for the 50 hp data.
Figure 51 Measured and simulated speed data for two 50 hp load inertia cases, I1 and I3. The parameter estimation data is I1 and the validation data is I3. Note that the measured and simulated curves lay on top of each other for both load inertia cases.
4.4.2.3 75 hp Motor Set-Up

Figure 53 and 54 shows similar data sets as Fig 49 and Fig 50, but for the 75 hp case. As with the 10 hp data, there is excellent agreement between the simulated and the measured data for the 75 hp data, but there is clearly torque ripple at the steady state condition and significant oscillation following a motor stop, as shown in Fig 54. The steady state torque ripple is most likely due to the water brake load not producing a constant torque. It is common for water brake loads to produce pressure oscillations within the load [48]. These pressure oscillations are what causes torque ripple. The magnitude of the ripple depends on the mechanical system dynamics.
Figure 53 Measured and simulated speed following a 75 hp motor stop using load inertia I1 and I3

Figure 54 Measured and simulated shaft torque following a 10 hp motor stop using load inertia I1 and I3
4.4.3 Motor Model and Dynamic Load Model – Motor Start

The results in this section show simulated induction motor behavior. The dynamic load model is included in the model. Results are presented using motor manufacturer parameters with the standard motor model, estimated motor parameters for the standard motor model, and estimated parameters for the non-linear motor model. The speed, stator current, and shaft torque is shown for each of the three motors. For clarity, only the phase C stator current is shown. The remaining two phases behave similar to phase C.

4.4.3.1 10 hp Motor Simulations

Figure 55 shows the speed waveform for a 10 hp motor during a start condition using load inertia $I_1$. At time $t=0$, the motor is energized from a standstill. Four curves are shown: a simulation using the standard motor model with motor manufacturer parameters, a simulation using the standard motor model with estimated parameters, a simulation using the non-linear motor model with estimated parameters, and measured data. For all three simulation curves, the dynamic load model using inertia $I_1$ was used.

The non-linear motor model produces the best agreement with the measured data and the standard model with the motor manufacturer parameters produces the worst agreement. The most noticeable characteristic of the standard motor model is that the simulated speed is more “concave” whereas the measured speed is more “convex”. Also, the standard motor model using the motor manufacturer parameters reaches rated speed much more rapidly than expected. This is because the motor manufacturer parameters cause the motor to produce significantly more torque than the estimated parameters. However, the stator current is unreasonably high, nearly four times the measured value. This can be seen in the current and torque waveforms shown in Figs 56 and 57 respectively.
Figure 55 Measured and simulated speed data during a motor start for the 10 hp motor using load inertia I1. 1 – standard motor model with motor manufacturer parameters, 2 – standard motor model with estimated parameters, 3 – non-linear motor model with estimated
Figure 56 Simulated and measured stator current in Phase C during a 10 hp motor start using the non-linear motor model and the standard motor model.
4.4.3.2  50 hp Motor Simulations

The graphs presented in the previous section are recreated using the 50 hp motor and shown in Figs 58, 59, and 60. The results are similar to the 10 hp motor simulations, with the exception of a couple of notable differences. First, when using the motor manufacturer
parameters, the motor takes longer to accelerate to rated speed, whereas the 10 hp motor accelerated to rated speed more rapidly. Second, there is significantly more disagreement between the simulated results and the measured results during acceleration.

The reason that the simulation using the motor manufacturer parameters shows a longer acceleration time is because the shaft torque developed when using these parameters is significantly less than what the motor produces. Fig 60 shows the shaft torque for this case. Although the magnitude of the shaft torque during starting appears high, the average value is actually lower. The large oscillations are a result of the response of the dynamic load model.

Although the non-linear model is an improvement over the standard motor model, the non-linear model still needs improving. The differences between the non-linear motor model and the measured data are a result of the fact that equations (4.6) – (4.12) do not completely describe the stator and rotor leakage reactance terms and the rotor resistance term for the 50 hp motor tested. The motor produces more torque during starting and adjustments to these equations can improve the starting torque characteristic. It should be noted that the discrepancies are not a result of poorly estimated parameter values, but rather oversimplified variations of the stator and rotor parameters described by (4.6) – (4.12). Improvements to these equations will improve the modeled behavior of the motor.
Figure 58 Measured and simulated speed data during a motor start for the 50 hp motor using load inertia I1. 1 – standard motor model with motor manufacturer parameters, 2 – standard motor model with estimated parameters, 3 – non-linear motor model with estimated.
Figure 59 Simulated and measured stator current in Phase C during a 50 hp motor start using the non-linear motor model and the standard motor model
Figure 60 Simulated and measured shaft torque resulting from a 50 hp motor start using the non-linear motor model and the standard motor model

4.4.3.3 75 hp Motor Simulations

Simulations similar to those of the previous two sections are presented here for the 75 hp motor. Unfortunately, the motor manufacturer parameters were not provided with this motor.

Figures 61-63 show that good agreement between the non-linear motor model and the measured data is achieved. The non-linear motor model demonstrates considerable improvement over the standard motor model.
Figure 61 Measured and simulated speed data during a motor start for the 75 hp motor using load inertia $I_1$. 1 – standard motor model with estimated parameters, 2 – non-linear motor model with estimated parameters, 3 – Measured data.
Figure 62 Simulated and measured stator current in Phase C during a 75 hp motor start using the non-linear motor model and standard motor model
As with the dynamic load model, a new data set must be provided to validate the estimated parameters. Figure 64 shows the measured and simulated speed data during a motor start using the 50 hp dynamic load model and load inertia $I_3$. In this case, load inertia $I_3$ is a factor of over 3.5 larger than load inertia $I_1$. The simulation data is produced by the non-linear motor model with estimated parameters. As expected with a greater inertia, the load
inertia I3 case requires over twice the time to accelerate to rated speed as the load inertia I1 case. However, the agreement between the simulated data and the measured data is similar to the agreement between the same simulated and measured condition for the load inertia I1 case.

As expected, similar results are obtained for the 10 hp motor and the 75 hp motor.

![Figure 64 Motor speed during a start of a 50 hp motor from a standstill using modified motor model and dynamic load model with load inertia I3](image)

**4.4.4 Motor Model and Dynamic Load Model - Momentary Service Interruption**

The results presented in the previous section show that the induction motor model and the dynamic load model produce good motor starting and stopping transient results when estimated parameters are used. The focus of this study however, is on momentary service interruptions. This section presents simulations during momentary service interruptions.

4.4.4.1 Momentary Service Interruption - Transient

The interruption duration that caused the worst case torque transient for the 50 hp motor with load inertia I was measured in the lab to be 0.135 seconds, or 8.1 cycles. This interruption duration is simulated using the non-linear motor model with the dynamic load model. At time t=0, the stator voltage, interrupting the stator current. 8.1 cycles later, the stator voltage is re-applied. The results are shown in Fig 65.
For each graph in Fig 65, the measured data from the lab and the simulated data are shown. The simulated stator voltage agrees very well with the measured stator voltage. All of the waveforms agree well until the stator voltage is re-applied at time 0.135 s. After 0.135 s, there is some discrepancy in the stator current, shaft torque, and speed. The most significant difference is in the magnitude of the shaft torque negative peak, where the simulated value is a factor of 3 less than the measured value.

Figure 65 Measured and simulated waveforms resulting from a 8.1 cycle momentary service interruption of a 50 hp motor using load inertia I1
In order to determine the reason for the discrepancy in the magnitude of the shaft torque negative peak, all of the data must be examined.

The most accurate measurement is the speed measurement. Since a 1024 ppr encoder is used for the speed acquisition, the speed measurement has very high noise immunity and is therefore the most reliable signal. The oscillation seen in the speed waveform is not noise, it is in fact a real oscillation due to the pressure oscillations generated in the water brake load, as previously mentioned.

The current measurement uses a hall effect current transducer rated for 2000A peak and was recently calibrated to NIST standards. Since the magnitude of the peak current is significantly less than 2000A, the current measurement is also assumed reliable.

The torque transducer measurement is probably the most questionable measurement, however, these tests use a state of the art torque transducer that guarantees transient performance within a bandwidth of 1.1kHz and high noise immunity [49]. The rating of the torque transducer is 100,000 in-lb, with a x2 overload rating. The magnitude of the peak torque measured for the 50 hp tests is less than 50,000 in-lb and the highest frequency component measured during the negative torque transient is less than 800 Hz. The shaft torque measurement is therefore assumed to be reliable. A closer look at the speed data reinforces this assumption.

The simulated speed waveform agrees well with the measured speed waveform, until the stator voltage is re-applied. At this point, the simulated speed waveforms predicts the shaft speed falling to about 1420 rpm, whereas the measured data measures a shaft speed of about 1350 rpm at the same instant in time. This is a difference of 70 rpm. In order for the simulated speed to fall an additional 70 rpm, a much larger electromagnetic torque must be generated.

The magnitude of the peak current measured in Phase C during the experimental testing is 775 A. The magnitude of the peak simulated current in Phase C is only 575 A. This is a difference of 200 A. If the simulated current peak increased to the value of the measured
current, clearly additional electromagnetic torque will be produced. It is reasonable to assume that the 200 A increase in the magnitude of the stator current will cause a significant increase in the electromagnetic torque developed. Additionally, it is reasonable to assume that the electromagnetic torque needed to cause the simulated speed to fall about 70 rpm will also cause the shaft torque magnitude to increase to the measured value. The discrepancy is therefore in the motor model.

The most likely reason that the magnitude of the simulated current is less than the magnitude of the measured current is that the stator leakage reactance variations and the rotor parameter variations are not correct. Correcting these parameter variations will most likely correct the 200 A Phase C peak current mis-match. It is also possible that the rotor leakage reactance saturates, which is not accounted for in the above non-linear model. Although the model used accounts for rotor leakage reactance variations with slip, it does not account for saturation, and this may have a significant effect on the electromagnetic torque developed.

Without detailed knowledge of the rotor construction, it is very difficult to obtain a highly accurate rotor model.

4.4.4.2 Momentary Service Interruption – Interruption Duration

As with the measurement data, the minimum speed, minimum and maximum torque, and the maximum current value are recorded for each of 100 interruption durations. This data is plotted for the 50 hp motor as a function of interruption duration in Fig 66. The measured data is included for comparison. Clearly the overall shape of the waveforms is consistent between the simulated and measured data, but as seen in Fig 65, the magnitude of the simulated quantities is less than the magnitude of the measured quantities.

The magnitude of the peak stator current is significantly higher in Fig 66 than in Fig 65. This is because only the Phase C current is displayed in Fig 65, and the peak value of all three phases is plotted in Fig 66. The general behavior of all three phases is the same, but the magnitude varies. However, for each phase, the simulated value is less than the measured value.
Figure 66 Simulated and measured minimum and maximum torque, maximum current magnitude, and minimum speed plotted as a function of interruption duration for a 50 hp motor using load inertia I1

Figures 67 and 68 show the results for the 10 hp motor and the 75 hp motor, respectively. Of the three motors, the simulated 75 hp motor results agree best with the measured data. However, the general trend of the magnitude of the simulated quantities being less than the measured quantities is still present.
Figure 67 Simulated and measured minimum and maximum torque, maximum current magnitude, and minimum speed plotted as a function of interruption duration for a 10 hp motor using load inertia I1
Figure 68 Simulated and measured minimum and maximum torque, maximum current magnitude, and minimum speed plotted as a function of interruption duration for a 75 hp motor using load inertia $I_1$.

### 4.5 STRESS ASSESSMENT

Although the magnitude of the simulated peak shaft torque transient is less than the measured peak shaft torque transient, the overall behavior of the induction motor model agrees well with the measurements. The interruption durations at which the worst case transients occur are accurately simulated. There is no indication that the magnitude of the transients should be larger than the measured values for the conditions tested. Therefore, the measured shaft torque is a good measure of the stresses generated in an induction motor during a momentary service interruption.

Much of prior work presented Section 2 use the electromagnetic torque produced by the motor as a gauge for the stresses produced during a momentary service interruption.
However, the electromagnetic torque is not the same as the shaft torque during transient operation. Usually the shaft torque will be less than the electromagnetic torque, as seen in Fig 69, which shows the electromagnetic torque, $T_{em}$, and the shaft torque, $T_s$ for the condition of a motor start. At time $t = 0$, the 50 hp induction motor is started from a standstill. $T_{em}$ is generated from the non-linear motor model. This graph shows the importance of including a dynamic load model with the motor model. Clearly there is a significant difference between the shaft torque and the electromagnetic torque during transient conditions.

![Graph showing simulated electromagnetic torque and shaft torque during a motor start of a 50 hp induction motor](image)

Figure 69 Simulated electromagnetic torque and shaft torque during a motor start of a 50 hp induction motor
Chapter 5

Simulation Based Stress Assessment of Motor Systems

Analysis of a single induction motor behavior during a momentary service interruption is useful but limited, because in industry it is very unlikely that a single motor will operate on its own system. Most likely several motors will be fed from the same source. General lighting and equipment loads may also be present on the system. During a momentary service interruption each motor produces a back-emf and if all of the motors remain connected to the line, and all of the general lighting and equipment loads remain connected to the line, then the motor back-emf interaction with the entire system must be considered.

5.1 SYSTEM DIAGRAM

Fig 70 shows a block diagram of a multi-motor system constructed in SimPowerSystems that can be used to study the behavior of a system of motors during a momentary service interruption. An ideal three-phase 480V source supplies power to the system through a series R-L line impedance. The power flow is controlled by a circuit breaker that feeds a bus. The 10 hp, 50 hp, and 75 hp induction motors presented above are all connected to the same bus. Additionally, a 1 kW resistive load connected to the bus and represents lighting or other non-motor equipment loads.

The motor model used for each motor is the standard motor model using estimated parameters. Although the agreement between the measured data is better with the non-linear motor model, the difference between the standard motor model and the non-linear motor model current, torque, and speed magnitude peaks are is not significantly different. Each motor is loaded by the dynamic load model with estimated parameters.
The values for the R-L line impedance model are the values that are estimated above. The system model therefore is similar to connecting the three motors and a resistive load to the power supply in the lab.

![Circuit Breaker R-L Line Impedance](image)

**Figure 70** Block diagram of a system used to study momentary service interruption behavior

### 5.2 SYSTEM SIMULATIONS

Fig 71 shows the results of simulations conducted on the system described in Fig 70. When the system is at a steady state condition, the circuit breaker is opened and then re-closed a short time later. The length of the interruption duration was varied from 1 to 100 cycles, in 0.01 cycle increments. The maximum current amplitude, minimum shaft torque, maximum shaft torque, and minimum speed were all recorded for each interruption duration. These points are plotted in Fig 71 as a function of interruption duration.

The interruption duration that causes the worst case torque transient for all three motors is 7.9 cycles. It is not surprising that all three motors experience the worst case torque transient at the same interruption duration. The back-emf of all three motors must be equal at any instant in time since the motors are connected in parallel. Therefore, when the circuit breaker re-closes, the back-emf of all three motors will be at the same phase angle. However, for a system of various size motors that are connected to various different loads, some motors will try to slow down faster than others. In order for the back-emf to remain equal across all motors, the motors that try to slow down will be accelerated by the other motors. Some of the motors are forced to become generators while other motors continue acting as a motor. This is seen by examining the electromagnetic torque produced by the motor.
Figure 71 Peak current, minimum shaft torque, maximum shaft torque, and minimum speed plotted as a function of interruption duration
Figure 72 shows the electromagnetic torque produced by each of the three motors illustrated in Fig 71 during a momentary service interruption lasting 7.9 cycles. At time t=0, the breaker is opened and 7.9 cycles (0.1317 s) later the breaker is re-closed. During the time from t=0 to t = 0.1317 seconds is when the interaction between the motors occurs. If the motor continues to act as a motor, the electromagnetic torque will remain positive. However, if the motor acts as a generator, the electromagnetic torque will become negative. A zoomed in view of Fig 72 is shown in Fig 73, where the positive and negative values of the electromagnetic torque are clearly seen.

As expected, the 10 hp motor continues to act as a motor. Since this is the smallest motor in the system, the energy storage capability is the least. On the other hand, the 75 hp motor is capable of storing much more energy and therefore acts as a generator, supplying the 1kW resistive load, the 10 hp motor, and at first the 50 hp motor. The 50 hp motor acts as a motor until about half way through the interruption, at which point the motor starts to behave as a generator.
Figure 72 Electromagnetic torque produced by the 10 hp, 50 hp, and 75 hp motor during a 7.9 cycle momentary service interruption
Figure 73: Zoomed in view of Fig 72 to show positive and negative electromagnetic torque during interruption.
Figure 74 shows the Phase A stator current in each of the three motors during the momentary service interruption. The current flow between the three motors is clearly seen while the breaker is open.

As the power rating of the resistive load increases, rate of decay of back-emf also decreases. This is because the resistive load absorbs power from the energy stored in the motor during the momentary service interruption. The lower the magnitude of the back-emf at the instant of reconnection, the lower the magnitude of the negative torque transient produced, as shown in Fig 32 of section 3.3.5.1. Therefore, in a practical application of a contactor ride-through device, the loads that remain connected to the supply during the interruption will draw power from the induction motors that also remain connected to the supply. This in turn leads to a lower magnitude torque transient at the moment of reconnection. If the load is very large, or there is a fault at the supply, the energy stored in the rotor may be transferred to the system very rapidly. In the case of a fault, a high magnitude negative torque transient can develop due to some or all of the motors being forced to supply a fault. However, previous literature shows that even a fault at the motor terminals will not produce negative shaft torque transients greater than the shaft torque transients produced during a momentary service interruption.

Additional tests on the motor system model indicated that it is possible for the system to “stall” if the line impedance is high and/or too many motors are allowed to ride-through the momentary service interruption. At the moment the power supply voltage returns, if the power demand on the system caused by many motors re-accelerating is high, the supply voltage will collapse and the motors will never re-accelerate.
Figure 74 Phase A stator current for all three motors illustrated in Fig 70 during a momentary service interruption.
The curves presented in Fig 71 appear very similar to the single motor data previously presented. Figure 75 shows a comparison between single motor testing and the motor system testing. The minimum torque produced is plotted as a function of interruption duration for the 50 hp motor. For clarity, the interruption duration only ranges from 1 to 21 cycles. Although not drastically different, the motor system simulations predict lower magnitude torque transients than the single motor tests. This is most likely because the 1 kW resistive load and the 10 hp motor has absorbed some of the energy stored in the 50 hp motor during the interruption.

![Figure 75 Minimum torque produced during single motor simulations and motor system simulations for the 50 hp motor](image)

It is interesting that the 50 hp motor peak shaft torque magnitude reaches the worst case at about the same time as the motor system reaches the worst case. However, this is just a coincidence. Figure 76 shows the peak shaft torque magnitude of all of the induction motors tested and the motor system simulations. The 10 hp motor reaches the worst case at a much
shorter duration than the 50 hp and the 75 hp. Likewise, the 75 hp motor reaches the worst case at a longer duration than the 10 hp and the 50 hp motor.

Figure 76 Magnitude of the peak negative torque transient generated during a momentary service interruption for the 10 hp, 50 hp, 75 hp, and motor system as a function of interruption duration. Inertia $I_1$ is used for all motors.
Chapter 6

Summary and Suggestions for Future Work

6.1 SUMMARY

Contactor ride-through devices are a desirable addition to any industrial facility. They can save time and money by preventing costly shut downs due to voltage sags and momentary service interruptions. Widespread use of contactor ride-through devices has been prevented by concerns over potentially damaging transients associated with allowing an induction motor to remain connected to the voltage supply during voltage sags or momentary service interruptions. The results presented here illustrate the worst possible current and torque transients developed when using a contactor ride-through device with a 10 hp, 50 hp, 75 hp, and a system of induction motors that are using torsionally stiff couplings and a variable inertia water brake load. No significant damage occurred during more than 10,000 experimental tests and further analysis shows that the magnitude of the torques generated are not high enough to cause shaft failure. Additionally, if contactor ride-through devices are used on a system of motors, the magnitude of the transients developed are even lower because the back-emf of each motor will decrease as power is fed to other loads connected to the supply.

It seems possible that motor manufacturer concerns over motor damage caused by the addition of a contactor ride-through device are overstated for the size motors included in this study. However, it should be noted that driven equipment typically found in industry was not evaluated during this study. The only driven equipment included in the testing was a custom-built variable inertia flywheel device and a power absorption unit used for the load. Although this equipment withstood the stresses generated by the current and torque transients during testing, typical industrial driven equipment may or may not be more sensitive to such stresses.
6.2 SUGGESTIONS FOR FUTURE WORK

The experimental data acquired for this study is very thorough and contains enough information for induction motor stress analysis and induction motor model parameter estimation. The dynamic load model is shown to have good agreement between the measured and simulated data and is therefore an accurate model of the system constructed in the lab. The parameter estimation method used accurately estimates the motor and load model parameters. The induction motor model, on the other hand, needs improving. The induction motor model does not accurately simulate the magnitude of the electromagnetic torque transient during a momentary service interruption. This is attributed to the non-linearities of the motor. Some of these non-linearities are accounted for in the study, which helps the accuracy of the overall motor behavior, but further improvements are needed.

In order to improve the model, a cross section of each motor rotor should be made so that the rotor bar geometry and construction is fully understood. The dimensions of the rotor bar construction can significantly aid the development of a non-linear rotor model [39]. Also, improvements in the stator leakage reactance parameter can be made by analyzing the construction of the stator windings.

Effects due to varying the spring constants of the couplings and the shafts should also be investigated. It is possible that different coupling and shaft assemblies will have a significant effect on the transient shaft torque response. Additionally, replacing the simple two mass dynamic model with a more complex multi-mass model may provide additional information about shaft torque stresses.

Further system studies are also needed. A case study on an actual industrial facility is a good way to extend this study to a practical application. Ideally, the system impedance would be measured and accounted for, in addition to all of the motors and loads. Measured voltage data during a momentary service interruption from the industrial facility could be used as the supply voltage for the model. Additional tests on various voltage supply disturbances could be conducted to fully understand the system behavior during a momentary service interruption.
REFERENCES


Appendix A

Reference Frame Transformations

**A.1 PARK’S TRANSFORMATION**

In the 1920’s, R.H. Park greatly simplified electric machine analysis by transforming, or referring, the stator quantities voltage, current, and flux linkage, to a fictitious two winding model rotating at the same speed as the rotor [37][38]. The greatest benefit of this transformation is that the time varying inductances due to electric circuits in relative motion and varying magnetic reluctance are eliminated for a synchronous machine. The transformation is as follows.

\[
f_{qdo} = \mathbf{K}_p f_{abc}
\]

where

\[
f_{qdo} = \begin{bmatrix} f_q \\ f_d \\ f_0 \end{bmatrix}
\]

\[
f_{abc} = \begin{bmatrix} f_a \\ f_b \\ f_c \end{bmatrix}
\]

and

\[
\mathbf{K}_p = \frac{2}{3} \begin{bmatrix}
\cos \theta & \cos \left( \theta - \frac{2\pi}{3} \right) & \cos \left( \theta + \frac{2\pi}{3} \right) \\
\sin \theta & \sin \left( \theta - \frac{2\pi}{3} \right) & \sin \left( \theta + \frac{2\pi}{3} \right) \\
\frac{1}{2} & \frac{1}{2} & \frac{1}{2}
\end{bmatrix}
\]

The inverse of \( \mathbf{K}_p \) is
In the above equations, $f$ can represent voltage, current, flux linkage, or electric charge. $\theta$ is the angle between the $a$-axis and the $q$-axis, as show in the diagram of Figure 77.

Figure 77 Relationship between abc reference frame and arbitrary qd0 reference frame. Figure 6.4 in [38].

The $q$-$d$ axis is a two winding equivalent model which rotates at an angular frequency of $\omega$. The rotor rotates at an angular frequency of $\omega_r$. The angle $\theta$ can be found by integrating the angular frequency $\omega$ over time, as follows.

$$\theta(t) = \int_0^t \omega(t) dt + \theta(0)$$  \hspace{1cm} (6)$$

where $\theta(0)$ is the value of the angle at the beginning of the integration. Similarly, the rotor angle can be found as follows.

$$\theta_r(t) = \int_0^t \omega_r dt + \theta_r(0)$$ \hspace{1cm} (7)$$
The angular frequency \( \omega \) may be chosen arbitrarily, but for Park’s transformation \( \omega \) is usually chosen as the frequency of the power supply voltage. This aligns the \( q \)-axis with the rotating \textit{ar} axis and is best suited for synchronous machine analysis. For induction motor analysis, \( \omega \) is usually chosen as zero, as first described by Stanley [37][38]. This aligns the \( q \)-axis with the stationary \textit{as} axis.

**A.2 STANLEY’S TRANSFORMATION**

In the 1930’s, H.C. Stanley applies Park’s Transform to induction machine analysis [37][38]. He let \( \omega \) equal zero, which refers the rotor quantities to a two winding equivalent model aligned with the stator. The greatest benefit of applying Stanley transformation to induction motor analysis is the elimination of the time varying inductances in the induction motor equations. Stanley’s transformation is also known as the stationary reference frame transformation.

From Equation 6, if \( \omega \) equals zero, then \( \theta \) equals zero and Equation 4 can be re-written as

\[
K_x = \frac{2}{3} \begin{bmatrix}
1 & \frac{1}{2} & -\frac{1}{2} \\
0 & -\frac{\sqrt{3}}{2} & \frac{\sqrt{3}}{2} \\
\frac{1}{2} & \frac{1}{2} & \frac{1}{2}
\end{bmatrix}
\]  

(8)

**A.3 THREE PHASE INDUCTION MOTOR MODEL**

Although many different types of induction motors exist, a common type is one comprised of a stationary stator winding and a rotating squirrel cage rotor winding [38]. The stator windings and the rotor windings form two separate circuits coupled magnetically. The circuit diagram describing these two windings is shown in Fig 78.
Figure 78. Winding diagram of a three-phase squirrel cage induction motor

The stator circuit is comprised of three wye connected windings, as shown on the left. A three-phase voltage source is externally connected to the as, bs, and cs terminals. The squirrel cage rotor, shown on the right, also consists of three wye connected windings. These windings are shorted together.

A.3.1 Induction Motor Equations in abc Frame

The stator winding voltages can be expressed as

\[ v_{as} = i_{as} r_s + \frac{d\lambda_{as}}{dt} \]  
\[ (9a) \]

\[ v_{bs} = i_{bs} r_s + \frac{d\lambda_{bs}}{dt} \]  
\[ (9b) \]

\[ v_{cs} = i_{cs} r_s + \frac{d\lambda_{cs}}{dt} \]  
\[ (9c) \]

\( v \) represents the voltage across each winding, \( i \) represents the current through each winding, \( r_s \) represents the winding resistance of each winding, and \( \lambda \) represents the flux linked by each winding. The rotor winding voltages can be expressed similarly as

\[ v_{ar} = i_{ar} r_r + \frac{d\lambda_{ar}}{dt} \]  
\[ (10a) \]

\[ v_{br} = i_{br} r_r + \frac{d\lambda_{br}}{dt} \]  
\[ (10b) \]
\[ v_{cr} = i_{cr} r_{r} + \frac{d\lambda_{cr}}{dt} \]  

(10c)

The stator winding and the rotor winding flux linkage equations may be written in compact matrix form as:

\[
\begin{bmatrix}
\dot{\lambda}_{s}^{abc} \\
\dot{\lambda}_{r}^{abc}
\end{bmatrix} =
\begin{bmatrix}
L_{ss}^{abc} & L_{sr}^{abc} \\
L_{rs}^{abc} & L_{rr}^{abc}
\end{bmatrix}
\begin{bmatrix}
i_{s}^{abc} \\
i_{r}^{abc}
\end{bmatrix}
\]

(11)

where

\[ \dot{\lambda}_{s}^{abc} =
\begin{bmatrix}
\dot{\lambda}_{as} \\
\dot{\lambda}_{bs} \\
\dot{\lambda}_{cs}
\end{bmatrix} \]  

(11a)

\[ \dot{\lambda}_{r}^{abc} =
\begin{bmatrix}
\dot{\lambda}_{ar} \\
\dot{\lambda}_{br} \\
\dot{\lambda}_{cr}
\end{bmatrix} \]  

(11b)

\[ i_{s}^{abc} =
\begin{bmatrix}
i_{as} \\
i_{bs} \\
i_{cs}
\end{bmatrix} \]  

(11c)

\[ i_{r}^{abc} =
\begin{bmatrix}
i_{ar} \\
i_{br} \\
i_{cr}
\end{bmatrix} \]  

(11d)

The inductance sub-matrices in Equation 5 contain the stator-to-stator, stator-to-rotor, rotor-to-stator, and rotor-rotor inductances. The stator-to-stator and the rotor-to-rotor inductances are not a function of the rotor position and can be expressed as:

\[ L_{ss}^{abc} =
\begin{bmatrix}
L_{ls} + L_{ls} & L_{lm} & L_{lm} \\
L_{lm} & L_{ls} + L_{ls} & L_{lm} \\
L_{lm} & L_{lm} & L_{ls} + L_{ls}
\end{bmatrix} \]  

(11e)

\[ L_{rr}^{abc} =
\begin{bmatrix}
L_{lr} + L_{lr} & L_{lm} & L_{lm} \\
L_{lm} & L_{lr} + L_{lr} & L_{lm} \\
L_{lm} & L_{lm} & L_{lr} + L_{lr}
\end{bmatrix} \]  

(11f)
The stator-to-rotor and the rotor-to-stator inductances are mutual inductances and are a function of the rotor position, as shown in Equation 11g.

\[
L_{sr}^{abc} = L_{rs}^{abc} = L_{sr} = \begin{bmatrix}
\cos(\theta_r) & \cos(\theta_r + \frac{2\pi}{3}) & \cos(\theta_r - \frac{2\pi}{3}) \\
\cos(\theta_r - \frac{2\pi}{3}) & \cos(\theta_r) & \cos(\theta_r + \frac{2\pi}{3}) \\
\cos(\theta_r + \frac{2\pi}{3}) & \cos(\theta_r - \frac{2\pi}{3}) & \cos(\theta_r) 
\end{bmatrix}
\]  

(11g)

In Equations 11e and 11f, \(L_{ls}\) is the per phase stator winding leakage inductance, \(L_{lr}\) is the per phase rotor winding leakage inductance, \(L_{ss}\) is the self-inductance of the stator winding, \(L_{rr}\) is the self-inductance of the rotor winding, \(L_{sm}\) is the mutual inductance between stator windings, \(L_{rm}\) is the mutual inductance between rotor windings, \(L_{sr}\) is the peak value of the stator-to-rotor mutual inductance, and \(\theta_r\) is the angle between the stator a-phase axis and the rotor a-phase axis, as shown in Figure 18. The superscript \(t\) in Equation 11g represents the transpose of the matrix.

Solving Equations 9 and 10 is extremely complex due to the dependence on the rotor position. Stanley’s transformation is well suited for this case, since the transformation eliminates the dependence on the rotor position.

**A.3.2 Induction Motor Equations in dq0 Frame**

The stationary reference frame described by Stanley is the reference frame of choice for induction motor transient analysis. In the stationary reference frame, the \(q\)-axis and the \(as\)-axis shown in Figure 18 will be aligned, since \(\omega = 0\). The voltage, current, and flux linkage quantities may be transformed to the stationary dq0 frame by applying the transformation of Equation 1 and Equation 8. The stator voltage equations in matrix form become

\[
\mathbf{v}_{s}^{dq0} = \mathbf{K}_s \frac{d}{dt} (\mathbf{K}_s)^{-1} \lambda_s^{dq0} + \mathbf{K}_s \mathbf{r}_s^{abc} (\mathbf{K}_s)^{-1} \mathbf{i}_s^{dq0}
\]  

(12)

Applying the transformations and re-writing Equation 12 in expanded form, the stator voltage equations in the stationary reference frame become
\[ v_{qs}^s = r_s i_{qs}^s + \frac{d\lambda_{qs}^s}{dt} \]  \hspace{1cm} (13a) \\
\[ v_{ds}^s = r_s i_{ds}^s + \frac{d\lambda_{ds}^s}{dt} \] \hspace{1cm} (13b) \\
\[ v_{0s} = r_s i_{0s} + \frac{d\lambda_{0s}}{dt} \] \hspace{1cm} (13c)

The rotor quantities can likewise be transformed and expressed in the stationary reference frame as

\[ v_{qs}^s = r_s i_{qs}^s + \frac{d\lambda_{qs}^s}{dt} - \omega \lambda_{dr}^s \] \hspace{1cm} (14a) \\
\[ v_{ds}^s = r_s i_{ds}^s + \frac{d\lambda_{ds}^s}{dt} - \omega \lambda_{qr}^s \] \hspace{1cm} (14b) \\
\[ v_{0r} = r_s i_{0r} + \frac{d\lambda_{0r}}{dt} \] \hspace{1cm} (14c)

Finally, the transformation of the flux linkages from Equation 12 results in

\[
\begin{bmatrix}
\lambda_{qs}^s \\
\lambda_{dr}^s \\
\lambda_{0s} \\
\lambda_{qr}^s \\
\lambda_{dr}^s \\
\lambda_{0r}
\end{bmatrix} = \frac{1}{\omega_p} \begin{bmatrix}
x_{ts} + x_m & 0 & 0 & x_m & 0 & 0 \\
0 & x_{ts} + x_m & 0 & 0 & x_m & 0 \\
0 & 0 & x_{ts} & 0 & 0 & 0 \\
x_m & 0 & 0 & x_{ts} + x_m & 0 & 0 \\
0 & x_m & 0 & 0 & x_{ts} + x_m & 0 \\
0 & 0 & 0 & 0 & x_{ts} & 0
\end{bmatrix} \begin{bmatrix}
i_{qs}^s \\
i_{dr}^s \\
i_{0s} \\
i_{qr}^s \\
i_{dr}^s \\
i_{0r}
\end{bmatrix} \] \hspace{1cm} (15)

where \( \omega_p = 2\pi f_{\text{rated}} \) and \( f_{\text{rated}} \) is the power supply system frequency. Note that although the flux linkages are a function of the base frequency, which is constant, the dependence on the rotor position has been eliminated.

The electromagnetic torque developed by the induction motor can be found from the power input. The sum of the instantaneous power supplied to the six stator and rotor windings can be expressed as

\[ p_{in} = v_a i_{as} + v_b i_{bs} + v_c i_{cs} + v_a i_{ar} + v_b i_{br} + v_c i_{cr} \] \hspace{1cm} (16)
Equation 1 and Equation 8 can be used to transform Equation 16 to the stationary reference frame, as follows.

\[ p_{in} = \frac{3}{2} \left( v_{qs} i_{qs} + v_{ds} i_{ds} + 2v_{0s} i_{0s} + v_{qr} i_{qr} + v_{dr} i_{dr} + 2v_{0r} i_{0r} \right) \]  \hspace{1cm} (17)

Equations 13 and 14 can be used to substitute for the voltage quantities in Equation 17. The electromagnetic torque can then be found by dividing the rate of energy converted to mechanical work \((\omega\lambda i)\) by the mechanical speed \((2\omega/\nu)\), where \(\nu\) is the number of poles. Dividing these two quantities and reducing the equation results in

\[ T_{em} = \frac{3}{2} \frac{P}{2} \left( \lambda_{ds} i_{qs} - \lambda_{qs} i_{ds} \right) \]  \hspace{1cm} (18)

The rotor speed can be found by equating the inertia torque to the accelerating torque

\[ J \frac{d\omega_m}{dt} = T_{em} - T_{mech} - T_{damp} \]  \hspace{1cm} (19)

where \(J\) is the inertia, \(\omega_m\) is the mechanical rotor speed, \(T_{mech}\) is the externally applied mechanical torque in the direction opposite the rotor speed, and \(T_{damp}\) is the damping torque opposite the direction of rotation.
The power disturbance generator is not only useful for the tests presented here, but can also be used for identifying equipment that is adversely affected by voltage disturbances. The power disturbance generator is significantly less expensive than existing power disturbance generators, has a significantly higher power rating, can operate indoors, and is portable so it can be easily transported and maneuvered around most industrial facilities. The voltage rating is 600Vac and the current rating is 400Aac continuous, 5kAac for 10ms. However, these ratings can easily be increased without sacrificing portability. The drop-out time, the pick-up time, and the interruption duration are all point-on-wave (POW) adjustable in 0.01 cycle increments.

**B.1 Control Circuit**

A 600V\textsubscript{rms} to 12V\textsubscript{rms} control transformer, XFMR1, is used to provide the reference between the power circuit and the control circuit. Specifically, the supply voltage reference waveform is the voltage measured at Phase A with respect to Phase B (Phase A-B). From this reference waveform, two clock signals are generated, a 60Hz clock signal and a 6kHz clock signal. The 60Hz clock signal is generated by feeding the supply voltage reference waveform into a zero-crossing detector, which is followed by an opto-isolator. In this case, the opto-isolator is used only to help with noise immunity. It’s not intended to provide additional isolation. Transformer XMFR1 provides the necessary isolation between the control circuit and power circuit and is also the device which limits the voltage rating to 600Vac. The voltage rating of the IGCTs is 4.5kV, so simply installing a different transformer will increase the voltage rating substantially.

The 6kHz clock signal is generated by feeding the 60Hz clock signal into a phase-lock-loop (PLL), which is used as a times 100 frequency multiplier. One 60Hz clock cycle corresponds with one cycle of the power circuit supply voltage and one cycle of the 6kHz clock cycle corresponds with 0.01 of a cycle of the power circuit supply voltage.
The timing for the total interruption duration is performed by three programmable counters. One counter controls the drop-out time, the point on the supply voltage reference waveform that the IGCTs open. A second counter controls the interruption duration, the length of time that the IGCTs remain open. A third counter controls the pick-up time, the point on the supply voltage reference waveform that the IGCTs close.

The 6kHz clock signal is used as the clock for the drop-out counter and the pick-up counter and the 60Hz clock signal is used as the clock for the duration counter. This means that the drop-out and pick-up counters count 100 times faster than the duration counter. The duration counter counts in cycles, the drop-out and pick-up counter count in 0.01 cycles.

1. **B.2 Solid State Switch Schematic**

Fig 21 shows the solid state switch circuit diagram for one phase. All three phases are configured identically. Momentary service interruptions are generated by using an IGCT as a switch. The IGCT is controlled by a fiber optic line and is a forward conducting, forward blocking device. Diodes D1, D2, D3, and D4 are therefore needed to allow the IGCT to be used with alternating current. During the positive half cycle, current flows from the supply, through D1, through IGCT1, through D2, and to the load. During the negative half cycle, current flows from the load, through D3, through IGCT1, through D4, and to the supply. Diode D5, resistor R1, and capacitor C1 make up the turn-off snubber circuit. A turn on snubber is not needed because of the inherent inductance in the line and in the load.

Switch SW1 and resistor R2 are used to generate voltage sags. When a momentary service interruption is desired, SW1 is left open, so no current flows through R2. However, if a voltage sag is desired, SW1 is closed so that R2 is effectively inserted in series with the load when IGCT1 is open. This creates a voltage divider between R2 and the load.

During voltage sag operation, the value of the resistor R2 shown in Fig 79 determines the sag level. If the load is a balanced three-phase load, choosing identical resistor values for all three phases will produce a balanced three-phase sag. However, different values may be chosen to produce unbalanced sags.
Resistor R2 is chosen based on the application. A high current load would require a much lower resistance value than a low current load would for a given sag depth. For this reason, R2 is easily replaceable. The selection of R2 is based on the full load current and rated voltage. An estimate of the equivalent impedance can be made by dividing the rated voltage by the full load current. Once this impedance is known, a range of resistances can be determined for a range of voltage sag depths using a simple voltage divider.

For voltage sag operation verification and field trials, high power multi-tap crane braking resistors were used as R2. However, studies suggest that the pulse power rating of resistors can be much higher than the continuous power rating [50][51]. Therefore, it’s possible to use much smaller resistors, potentiometers, or rheostats if the pulse power rating is known.

Figure 79 Circuit diagram of the solid state switch for one phase

Figure 80 shows a photograph of the power disturbance generator with the front cover removed. IGCT1 and D1-D4 from Fig 79 are labeled. The two IGCT’s to the right of
IGCT1 and the eight diodes to the right of D1-D4 comprise the solid state switches for the remaining two phases. The programmable controller sits between the data acquisition laptop computer and the automation laptop computer.

Figure 80 Photograph of the portable power disturbance generator

B.3 Switching Stresses

Before any momentary service interruption testing began, the power disturbance generator was tested for proper POW operation and switching stresses. These tests were conducted using a three-phase 460V, 1780RPM, 150 hp Reliance Electric squirrel cage induction motor as a load. Phase to phase supply voltages, phase to phase motor voltages, supply current, IGCT voltage, IGCT current, snubber diode current, and snubber capacitor voltage are all
monitored and recorded. LEM Hall-effect current transformers are used to monitor the currents and high voltage oscilloscope probes are used to monitor the voltages. A Nicolet Multi-Pro transient analyzer sampling at 1MHz. is used to record the data.

Figure 81 shows the results of interrupting the current at a positive current peak in phase A. Supply voltage, motor voltage, IGCT voltage, IGCT current, snubber diode current, and snubber capacitor voltage are all reported. Just before time t=0, the phase A current is at a positive peak of 273A. At time t=0, the POW switch turns the IGCTs off and the current begins decreasing. As the current drops, the voltage across the IGCT increases and the snubber capacitor is charged by the current flowing through the snubber diode. The IGCT is designed as a snubberless device, therefore the fact that the voltage begins to rise before the current reaches zero is not a concern. This is typical IGCT operation [52]. However, even though IGCTs have an enlarged safe operating area (SOA), a dv/dt snubber is still required for this application. The inductance in the supply lines and the motor circuit is significant, so the energy stored in this inductance must be transferred somewhere other than the IGCT. As shown in Fig 81, the IGCT voltage reaches a peak value of 1100V during turn off. If the snubber circuit was not present, this voltage would almost certainly exceed the 4500V rating of the IGCT.

A turn on snubber is not needed because the inductance in the supply lines and the motor circuit is large enough to act as the turn on snubber. This is clearly shown in Fig. 82, where the phase A IGCT voltage and current is plotted vs. time as the IGCTs are turned on and the motor is energized at the phase A voltage peak. The IGCT voltage drops rapidly as the current increases slowly.
Figure 81 IGCT and snubber circuit turn-off switching waveforms. Interruption began at a positive current peak. Vs is supply voltage, Vm is motor voltage, Vigct is IGCT voltage, Iigct is IGCT current, Vc is snubber capacitor voltage, Id is snubber diode current.
Figure 82 IGCT turn-on switching waveforms. IGCT were turned on at a voltage positive voltage peak

**B.4 Momentary Service Interruption**

Figure 83 shows an 8.5 cycle momentary service interruption of a 75 hp 4 pole induction motor. The motor was running at rated current and voltage at the time of the interruption. The top graph shows the motor voltage and the bottom graph shows the motor current. At time = 0, the momentary service interruption is initiated by opening the IGCTs, thereby disconnecting the motor from the supply voltage. 8.5 cycles later, the IGCTs are closed, reconnecting the motor to the supply voltage.
Figure 83  Momentary service interruption lasting 8.5 cycles. Dropout is at zero crossing, pickup is 8.5 cycles later.

The 8.5 cycle momentary service interruption shown in Fig 83 is the exact interruption duration required for the back-emf of the motor to become 200 degrees out of phase with the supply voltage. Therefore, once the IGCTs close, the back-emf of the motor and the supply voltage are 200 degrees out of phase. This produces high magnitude current transients caused by the interaction of the stator circuit flux and the rotor circuit flux. The peak current recorded was 2,149 A. Several other tests were conducted that produced current peaks around 2,000 A, which had no adverse effects on the POW switch or controller.
C.1 Matlab Code for Initializing Motor and Finding Peaks

% Load three-phase induction motor parameters

clear;

% Initial conditions
Psiqso = 0.1;  % stator q-axis total flux linkage
Psipqro = 0.1; % rotor q-axis total flux linkage
Psidso = 0.1;  % stator d-axis total flux linkage
Psipdro = 0.1; % rotor d-axis total flux linkage
wrbywbo = 0.0; % pu rotor speed

%Parameters of 50 hp, three-phase induction machine
Sb = 50*746;   % rating in VA
Vrated = 480;  % rated line-to-line voltage in V
P= 4;         % number of poles
frated = 60;   % rated frequency in Hz
Irated = 58;

wb = 2*pi*frated;  % base electrical frequency
we=wb;
wbm = 2*wb/P;      % base mechanical frequency
Zb=Vrated*Vrated/Sb;  % base impedance in ohms
Vm = Vrated*sqrt(2/3);  % magnitude of phase voltage
Vb=Vm;
Tfactor = (3*P)/(4*wb);  % factor for torque expression

%Machine Parameters
rs = 0.1376;       % stator wdg resistance in ohms
xls_0 = 0.11761;   % stator leakage reactance in ohms
xplr_0 = 0.44759;  % rotor leakage reactance in ohms
xm = 19.487;       % stator magnetizing reactance in ohms
rpr_0 = 0.028493;  % referred rotor wdg resistance in ohms
drpr = 0.097547;
dxplr = 0.16003;
dxls = 1.7029e-005;
Dstray = 2.2452e-013;

Ipu = [1 1.14 1.223 1.385 1.78 2.0 2.2 3.0 4.0 5.0];
xls_variable = [0.127 0.127 0.127 0.127 0.1255 0.124 0.1148 0.0985 0.0845 0.0761];

% Inertias

Jm = 0.3889 + 0.07;  % motor inertia + motor coupling
\[ J_l = 0.0245 + 0.6614 + 0.07 + 0.0106; \% load + flywheel + load coupling + TTcoupling \]

\% Spring Constants

\[ k_{TT} = 2.4163 \times 10^5; \]
\[ C_{tml} = 25; \% 12.464; \]

\% Load Constants

\[ C_{tl} = 0.095782; \]
\[ R_{bl} = 2.3373 \times 10^{-14}; \]
\[ C_{tm} = 0.010729; \]
\[ R_{bm} = 2.3373 \times 10^{-14}; \]
\[ D_{omega} = 0.0051963; \]

\[ \text{for } i = 3.0146:0.0016666:4.6813 \quad \% \text{dropout time} = 2.9979 \text{ seconds.} \]
\[ \text{1 cycle after that} = 3.0146 \quad \text{1/10 of a cycle increments} = 0.00166666, \text{end of 100 cycles} = 4.6813 \]

\[ \text{pickup} = i; \quad \% \text{for the for loop, pickuptime} = i \]
\[ tstop = i + 0.5 \]
\[ [t, x, y] = \text{sim('PID_39_mindata_NA_NMS_LI_Peaks'}); \]
\[ \text{load peakdata.mat}; \]
\[ \text{data=ans;} \]
\[ [r, c] = \text{size(data)}; \% \text{need to skip first part before waveforms reach steady state} \]
\[ S = \text{data}(2,20000:c); \% \text{time is automatically written to row 1} \]
\[ T = \text{data}(3,20000:c); \]
\[ I1 = \text{data}(4,20000:c); \]
\[ I2 = \text{data}(5,20000:c); \]
\[ I3 = \text{data}(6,20000:c); \]

\[ \text{I1=abs(I1);} \]
\[ \text{I2=abs(I2);} \]
\[ \text{I3=abs(I3);} \]

\[ \text{currentmax1=max(I1);} \]
\[ \text{currentmax2=max(I2);} \]
\[ \text{currentmax3=max(I3);} \]

\[ \text{currentmaxtemp} = [\text{currentmax1 currentmax2 currentmax3}]; \]

\[ \text{currentmax=max(currentmaxtemp);} \]
\[ \text{speedmin=min(S);} \]
\[ \text{torquemax=max(T);} \]
\[ \text{torquemin=min(T);} \]

\[ \text{peaks(:,1)=speedmin;} \]
\[ \text{peaks(:,2)=torquemax;} \]
\[ \text{peaks(:,3)=torquemin;} \]
\[ \text{peaks(:,4)=currentmax;} \]

\[ \text{datafile='50hpI1peaks.txt';} \]
C.2 50 hp Typical Load Parameter Trajectory

Figure 84 Parameter trajectory from dynamic load model parameter estimation process
Figure 85 Parameter trajectory from 50 hp motor non-linear motor model parameter estimation process
Figure 86 Simulink motor model
Figure 87 Q-axis block from simulink motor model in Fig 86
Figure 88 D-axis block from simulink motor model in Fig 86
Figure 89 Rotor block in simulink motor model in Fig 86. This is the diagram of the dynamic load model.
Figure 90 back-emf to qd0 voltage block in simulink motor model in Fig 86

Figure 91 abc - qd0 current block in simulink motor model in Fig 86
Figure 92 abc to qd0 voltage block in simulink motor model in Fig 86
Figure 93 zero sequence block in simulink motor model in Fig 86
C.5 Motor System Model

Figure 94 SimPowerSystems motor system model